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BRITISH WELDING JOURNAL

1961 JUNE

VOL. 8 NO. 6

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CONTENTS

Welded Maidenhead Bridge
By Gilbert Roberts

Weldability of Aluminium
By G. Westendorp

Fabricated Pipe Bends
By P. H. R. Lane and R. T. Rose

Spot Welding Temperatures
By J. A. Greenwood

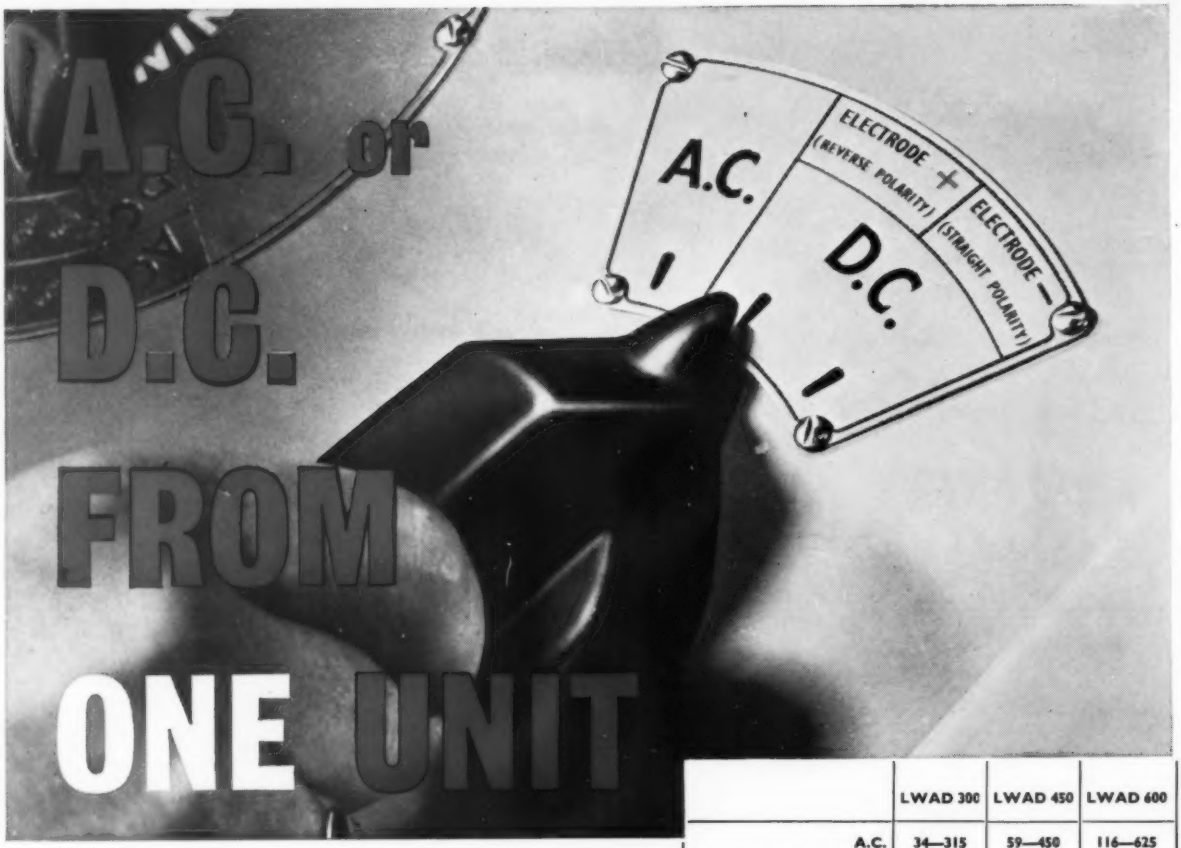
INTERNATIONAL WELDING

Current Sources for Welding

Pressure Vessel Brittle Fracture

Reports of Commissions

Cover Illustration
see p. 291



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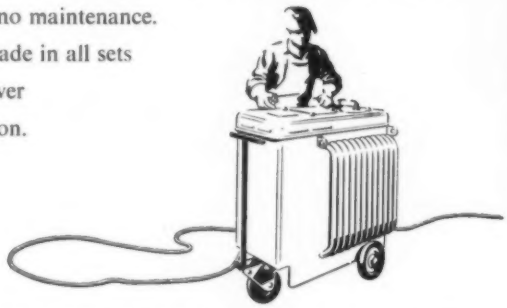

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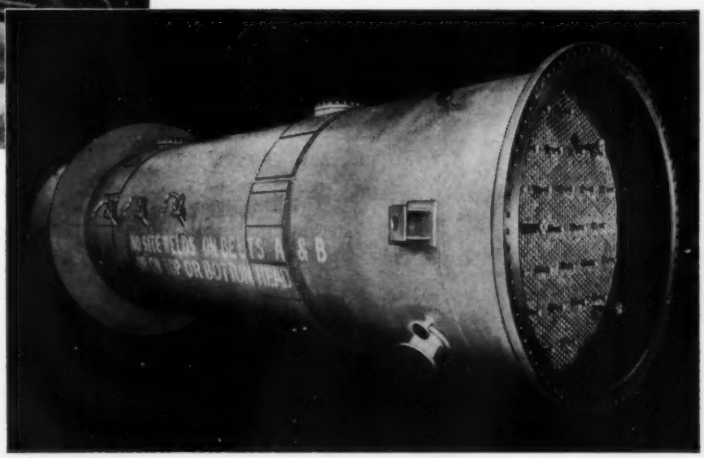
Below: This Ethylene Oxide Reactor weighs 135 tons, is 9 ft. 6 ins. in diameter and contains over 15 miles of 1¾ in. dia. catalyst tube.



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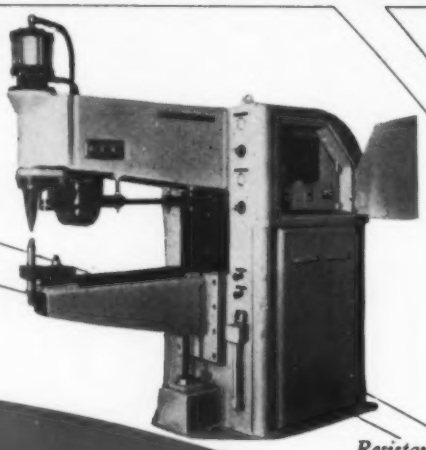
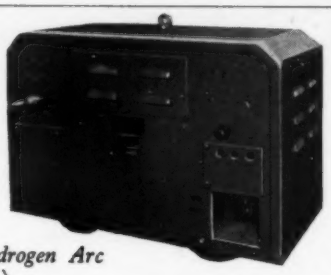


Resistance



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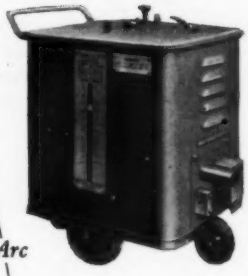

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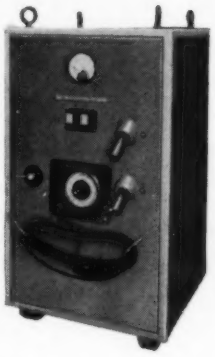

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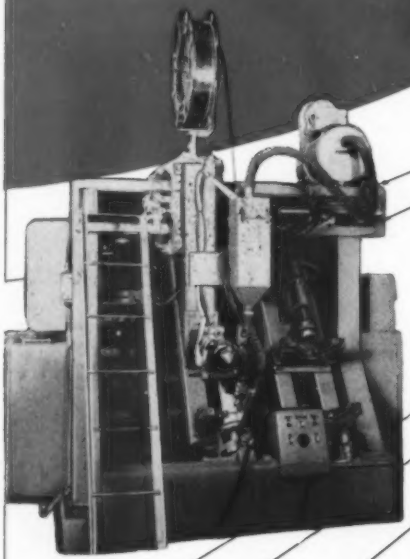

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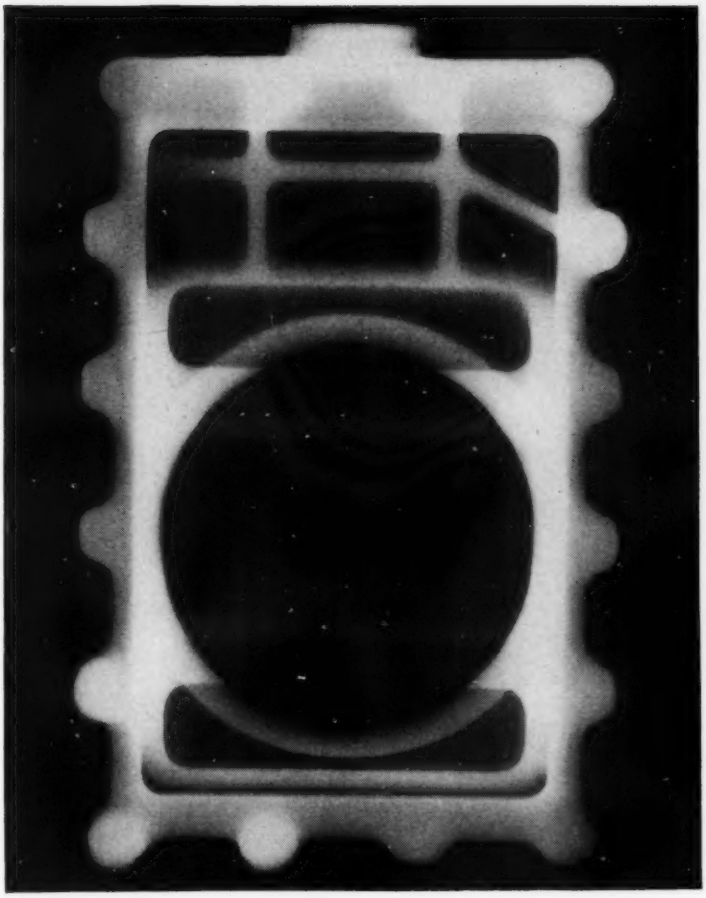

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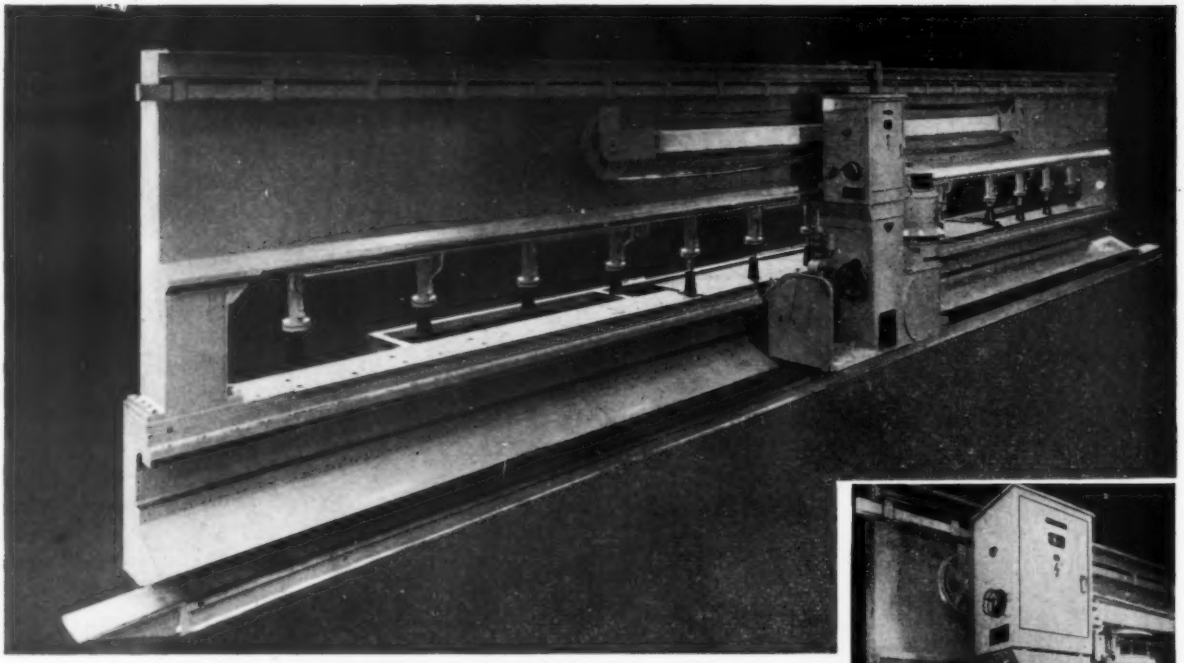

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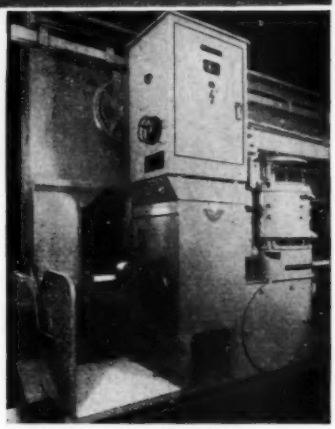

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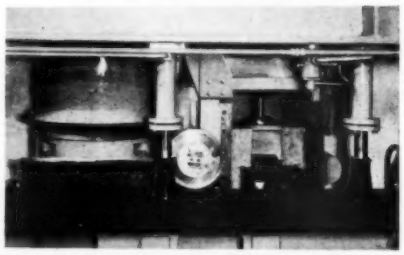

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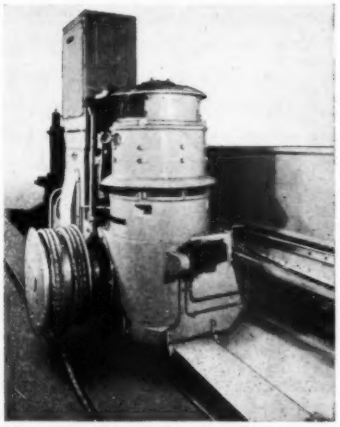

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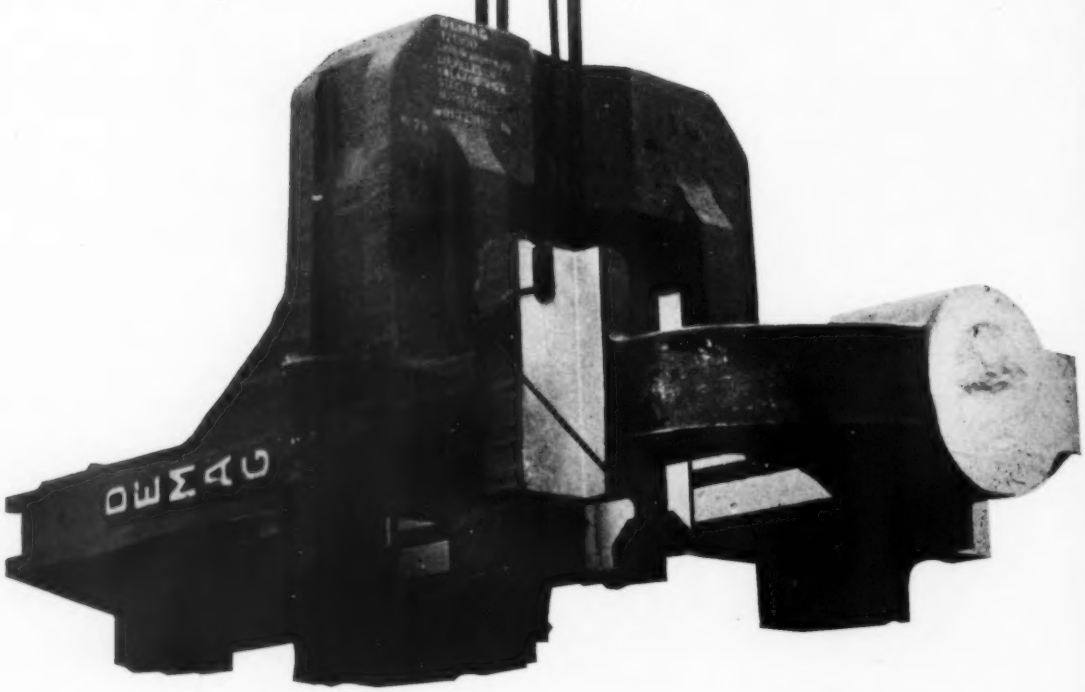

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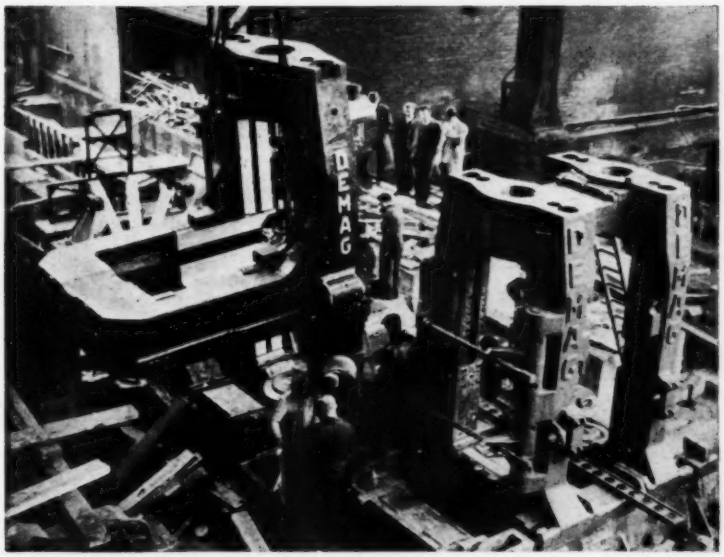

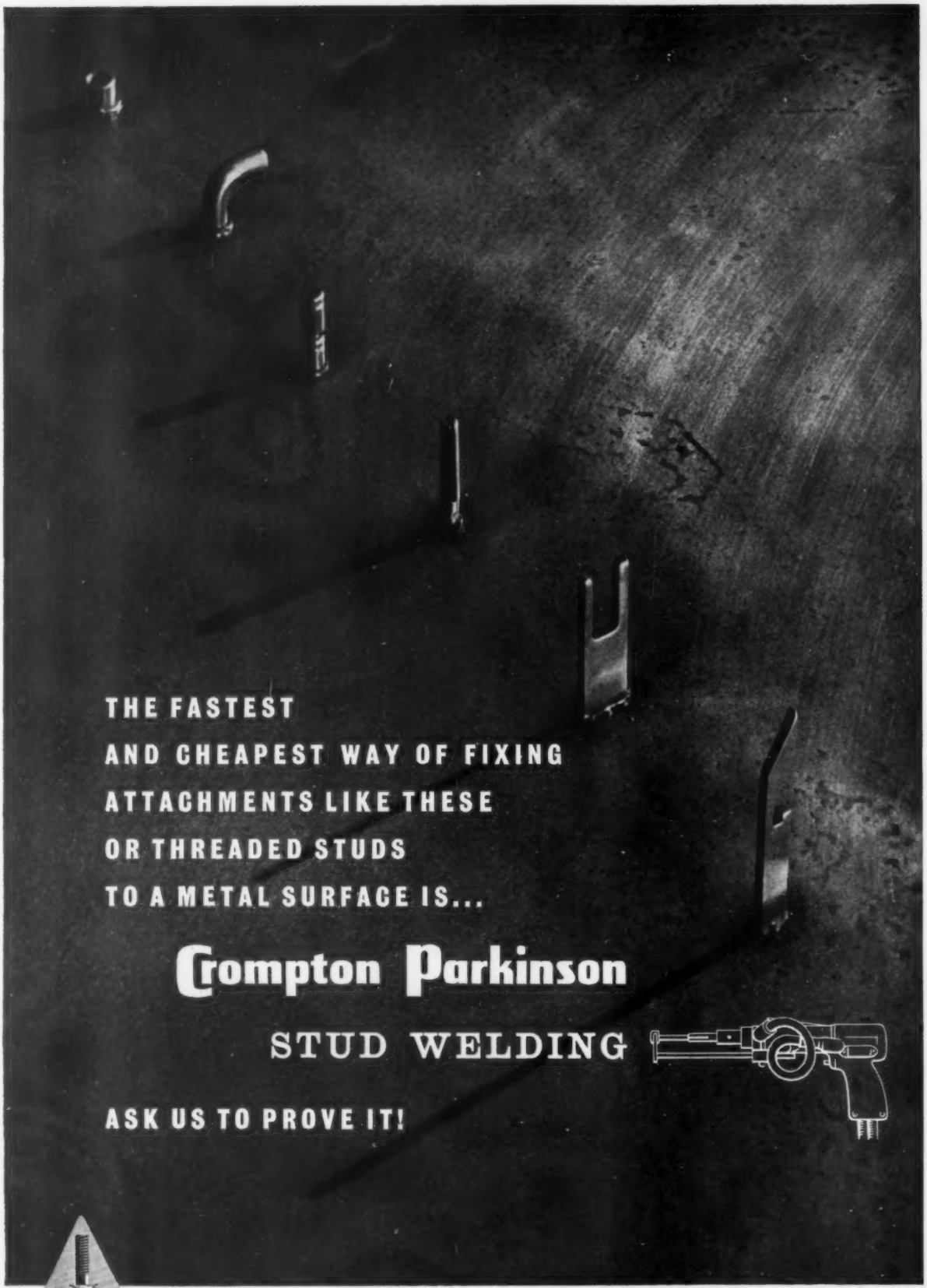
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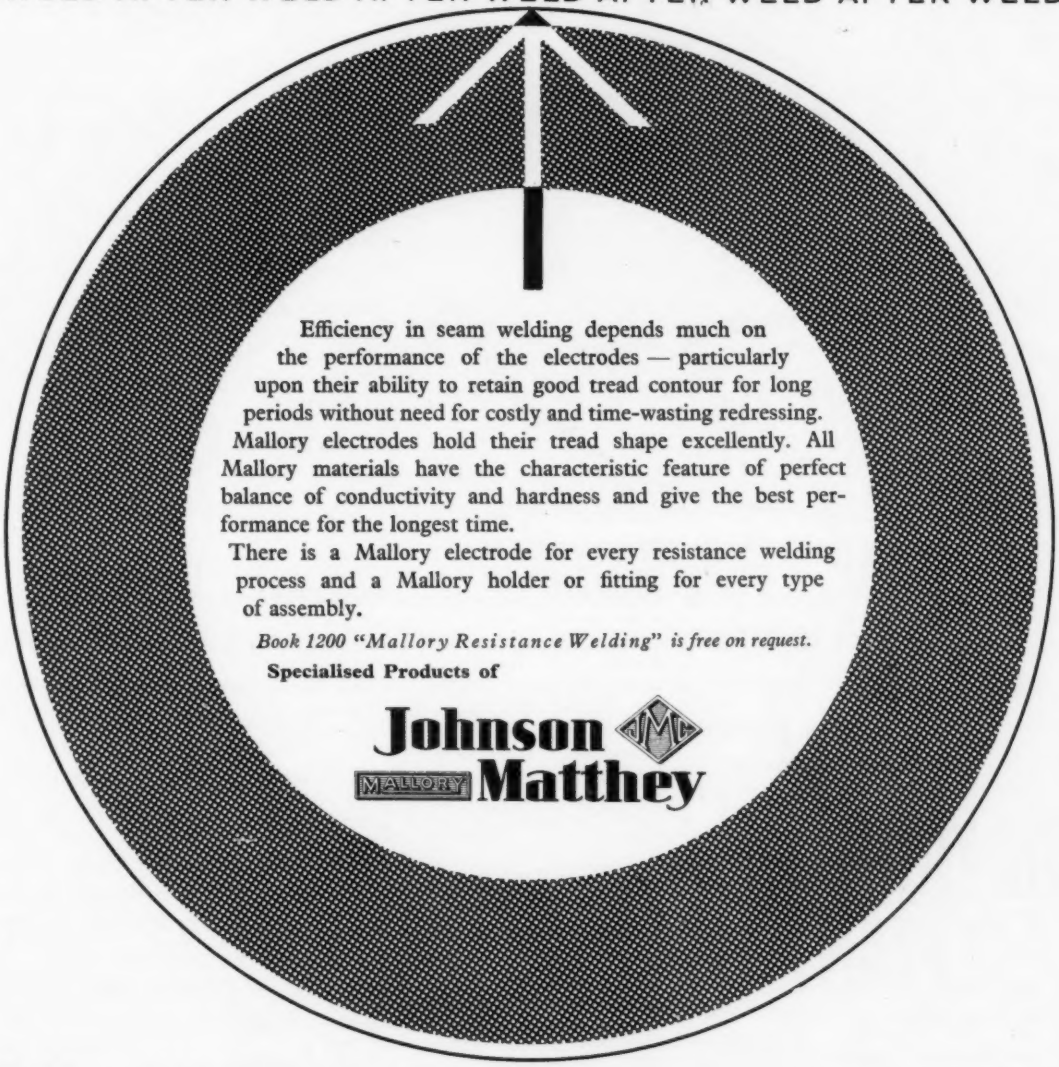

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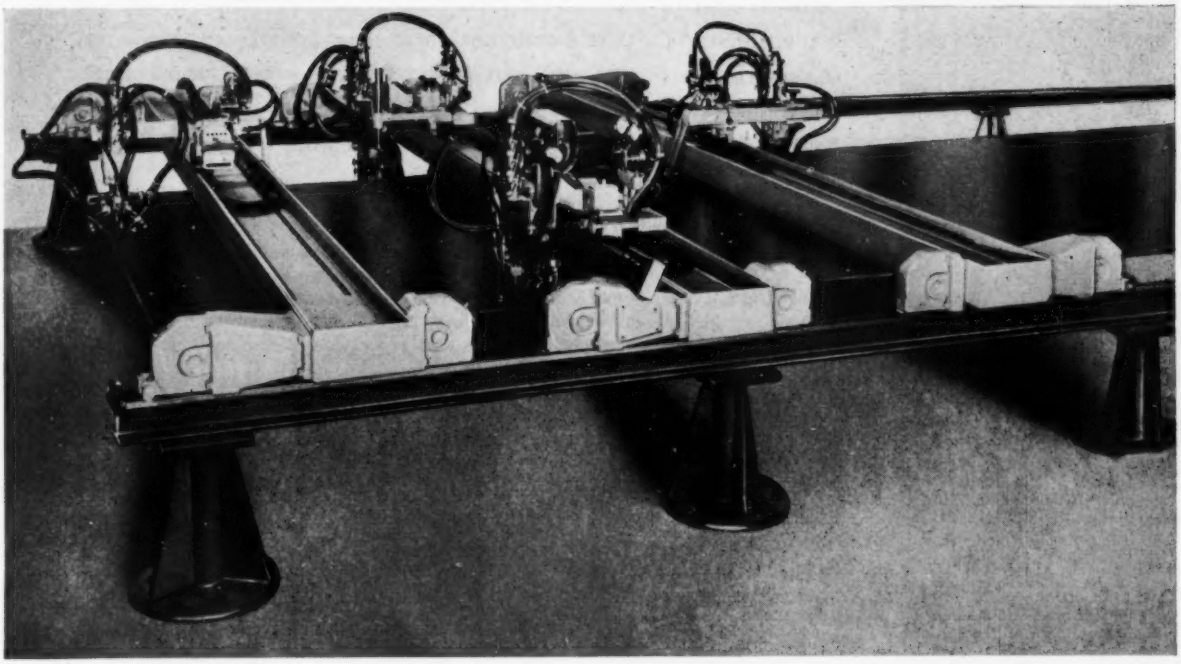

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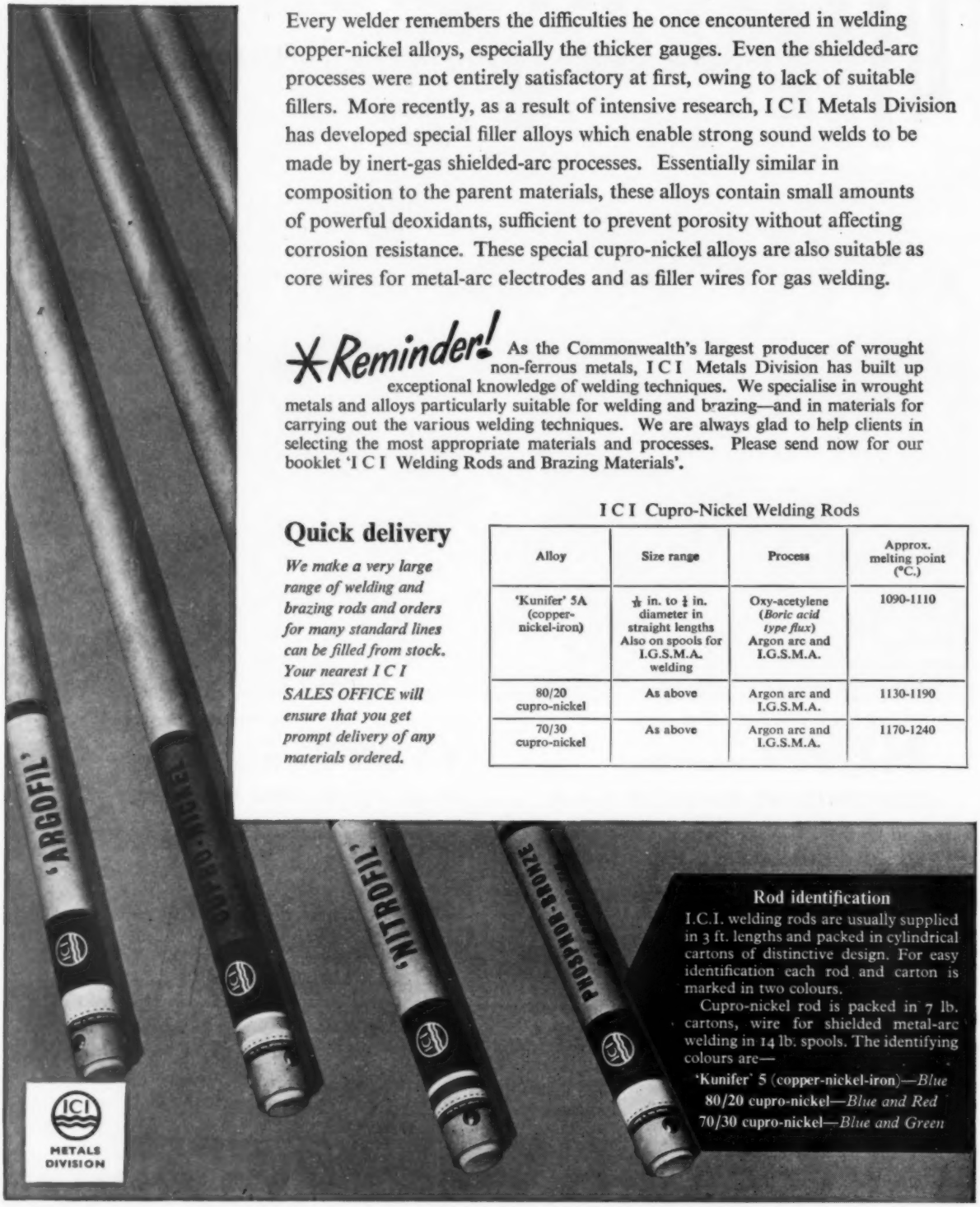
Welding Cupro-Nickel alloys



Every welder remembers the difficulties he once encountered in welding copper-nickel alloys, especially the thicker gauges. Even the shielded-arc processes were not entirely satisfactory at first, owing to lack of suitable fillers. More recently, as a result of intensive research, I C I Metals Division has developed special filler alloys which enable strong sound welds to be made by inert-gas shielded-arc processes. Essentially similar in composition to the parent materials, these alloys contain small amounts of powerful deoxidants, sufficient to prevent porosity without affecting corrosion resistance. These special cupro-nickel alloys are also suitable as core wires for metal-arc electrodes and as filler wires for gas welding.

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Alloy	Size range	Process	Approx. melting point (°C.)
'Kunifer' 5A (copper-nickel-iron)	$\frac{1}{16}$ in. to $\frac{1}{4}$ in. diameter in straight lengths Also on spools for I.G.S.M.A. welding	Oxy-acetylene (*Boric acid type flux*) Argon arc and I.G.S.M.A.	1090-1110
80/20 cupro-nickel	As above	Argon arc and I.G.S.M.A.	1130-1190
70/30 cupro-nickel	As above	Argon arc and I.G.S.M.A.	1170-1240

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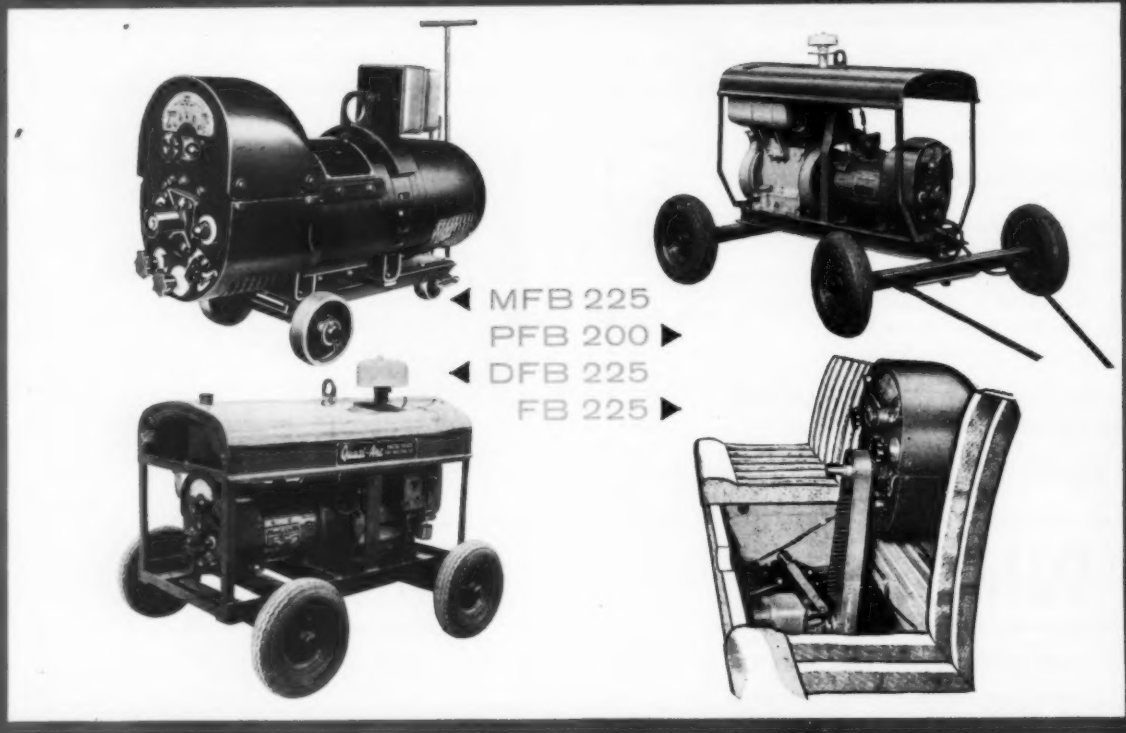

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CONTENTS

INSTITUTE OF WELDING

291 **The Welded Maidenhead Bridge**
By Gilbert Roberts (with Discussion)

310 **Some Remarks on the Weldability of Aluminium**
By G. Westendorp

315 **Letter to the Editor—Quality Control**

BRITISH WELDING RESEARCH ASSOCIATION

316 **Temperatures in Spot Welding**
By J. A. Greenwood

323 **Experiments on Fabricated Pipe Bends**
By P. H. R. Lane and R. T. Rose

335 NEWS AND ANNOUNCEMENTS

341 CURRENT WELDING LITERATURE

Supplement

INTERNATIONAL WELDING

29s **Characteristics of Current Sources for Special Arc Welding and Cutting Processes**

32s **Pressure Vessel Brittle Fracture and Information Required to Design against It**

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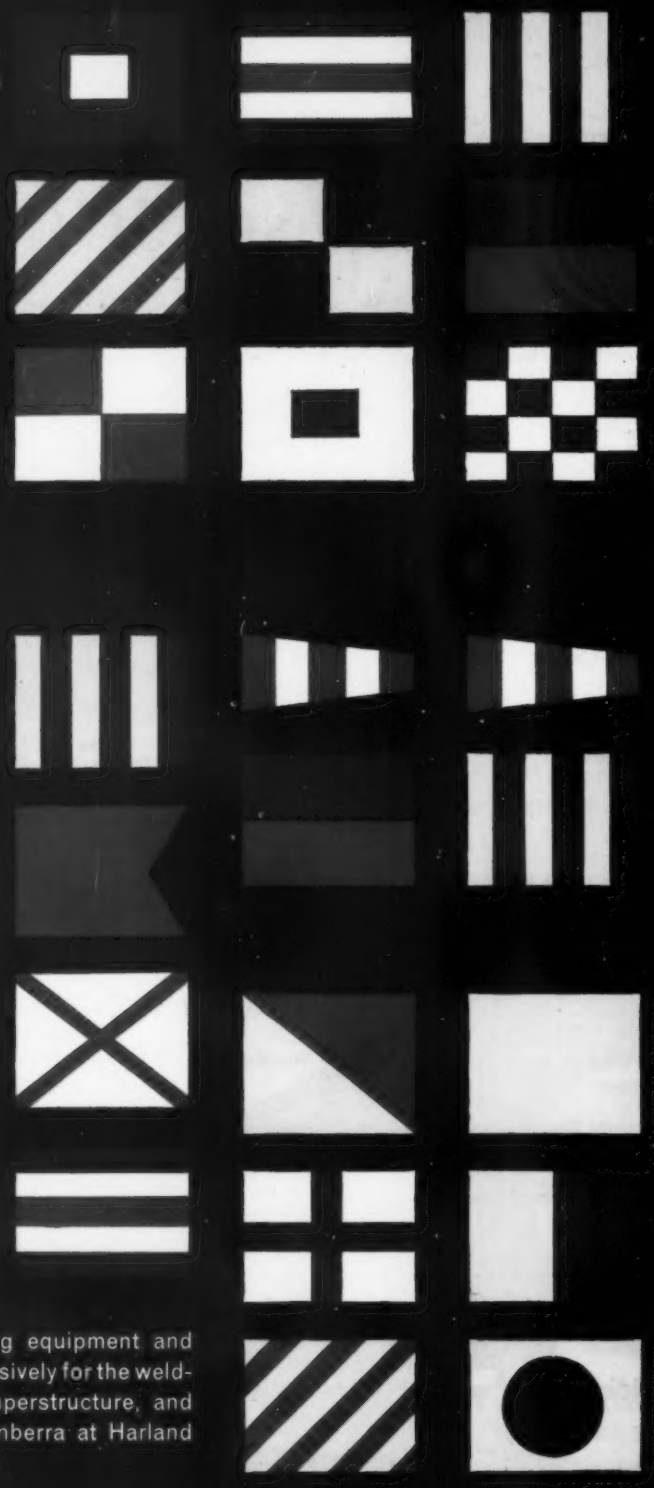

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The Welded Maidenhead Bridge

By Gilbert Roberts,

B.SC., M.I.C.E.

The following paper was presented at a Joint Meeting of the Institute with the South London Branch, at 54 Princes Gate, on Wednesday, 8th February, 1961, with Mr. H. C. Adams, M.C., M.I.C.E. (Ministry of Transport) in the Chair.

In introducing Mr. Roberts the Chairman referred to the great experience of the author in bridge design and construction and in the field of welding. In the early days of welded fabrication he was with Sir Douglas Fox and Partners when the design of the Sydney Harbour Bridge was being worked out, and later he was with Dorman Long when the construction proceeded.

In 1934 he joined Sir William Arrol and Company as chief engineer and later became a director. In 1949 he joined a group consisting of Mott, Hay and Anderson and Freeman Fox and Partners for the design of, and later to look after the construction of, the Severn Bridge, and he then became a partner in the firm of Freeman Fox and Partners.

THE new road bridge at Maidenhead, now almost completed and due to be opened in May 1961, was originally planned more than 25 years ago. In 1935 the Ministry of Transport appointed Messrs. Freeman, Fox and Partners as the consulting engineers for the river crossing, and the design was carried out by Mr. (later Sir) Ralph Freeman, senior partner of the firm and a Past-President of this Institute. I had worked for Sir Ralph on the Sydney Harbour Bridge design and although I was not then on his staff he asked me to do the calculations for the new bridge as a spare-time job.

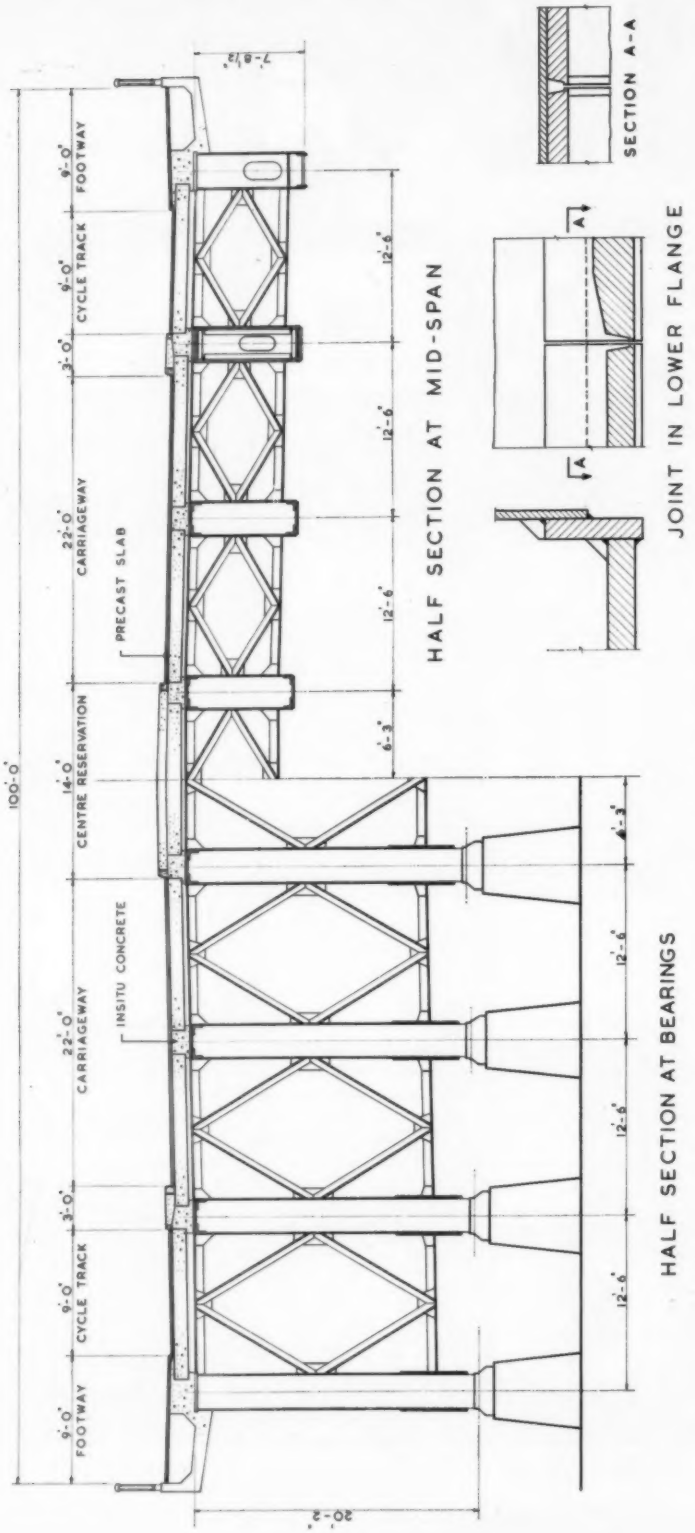
Original Design

The design proposed by Sir Ralph was a continuous girder bridge with spans of 38 ft, 270 ft, and 38 ft, with eight girders at 12 ft 6 in. centres, just as it is today. The girders were of box construction, 2 ft 6 in. wide and of a depth varying from 7 ft 6 in. at the centre to 20 ft over the main bearings (*see* Fig. 1). Six of the girders were designed for riveted construction and the outer girders were to be welded, as Sir Ralph was anxious to put into practice his knowledge of this novel form of construction. Also it was felt that welding would improve the external appearance of the bridge, and on architectural advice considerable pains were taken to give a panelled effect to the outer webs by making a vertical break in the plating at 6 ft intervals.

In the deeper parts of the section, where there is a horizontal joint in the girders, the outer web plates consisted of vertical strips 5 ft 9 in. wide running for the full depth without horizontal joints. Whether this panelling would, in fact, have been visible at all or any improvement on the present design is perhaps an open question, but it certainly involved a large increase in the amount of site welding. Sir Ralph's architectural advisers at that time also much preferred a semi-ellipse to the usual flat parabola for the soffit

This paper was presented at a Joint Meeting of the Institute with the South London Branch, on Wednesday, 8th February 1961.

The author is a partner of Freeman, Fox and Partners.



1—Cross section of 1938 design

of the bridge, and with much ingenuity in the flange details at the bearings this elliptical curve was faithfully incorporated, as it is today in the present design.

In this original design the deck was formed of precast reinforced concrete slabs about 11 ft square and 12 in. thick, with the reinforcing bars projecting from the edges of the slabs and incorporated with *in situ* concrete over the girders. Shear connectors on the top flanges of the girders ensured that the concrete would act with the steel, partially at least, in taking compressive stresses. The top flanges of the two welded girders could therefore be formed of a single universal plate, but the area required at the bottom flange at the centre of the bridge (as well as the requirement of the panelled effect) made it impossible to use a single rolled section and $7 \times 1\frac{1}{2}$ in. flats were added to each edge of the main plate. Full-size tests were made of the welding of this type of joint at the heaviest flange section, and there is no doubt that it would have been practicable, although somewhat cumbersome. All the steelwork was of mild steel since at that time the welding of heavy sections of high-tensile steel was not considered to be practicable.

When tenders were invited for the bridge in 1937 the author had recently joined Sir William Arrol and Company of Glasgow, and was responsible for preparing an erection scheme and making an estimate for the site welding. They secured the contract and sub-let the foundation and concrete work to L. J. Speight. The foundations and concrete abutments were completed in 1939, and all the 1600 tons of steel had been delivered to the shops and fabrication started when the war caused it to be laid aside for more urgent work. Most of the steel was, in fact, used for other jobs during the war period, and when in 1946 Arrolls wanted to re-start fabrication they were dismayed to find that no licence was forthcoming for new steel. Their contract was terminated and the concrete abutments remained as one of the curiosities of the Maidenhead district until, in 1959, a new contract for the superstructure was let, this time to the Horseley Bridge Company.

New Design

When, in 1958, the Ministry of Transport instructed the consulting engineers to continue the project the author had become a partner in Freeman, Fox and Partners, and it fell to him to review the design before the documents were issued for fresh tenders.

It was immediately apparent that engineering designs are not among those commodities, if any exist, that improve by keeping for 20 years in cold storage. What was once a novel and up-to-date steelwork design now looked decidedly *passé*. Also the Ministry's standard loading had changed, with the result that the original deck slabs were slightly overstressed and different road widths were now required. Some modernisation seemed necessary, but the original elevation and river clearances had to be retained, as well as the elliptical curve on the underside. Since the bearings and anchorages for the old arrangement were already in place it would have been difficult to vary the outline appreciably in any case. However, it was feasible to re-design the superstructure to take account of recent developments in qualities of materials, working stresses, and techniques of construction. Figure 2 shows the elevation of the new design and Fig. 3 the cross section.

The revisions may be summarised as follows:

(i) Higher strength concrete in a deck slab of reduced thickness, cast *in situ*
(ii) The adoption of single-web girders stiffened in accordance with the latest principles of web stiffening
(iii) The use of welding throughout, including site welding of all the girder joints, with high-strength bolts, acting in tension, for the bracing connections
(iv) The use of high-tensile steels of weldable quality for the girder webs and flanges, instead of mild steel.

It is the revised design, and especially the advantages accruing from the whole-hearted adoption of welding, that form the subject of this paper.

Materials

Reinforced concrete

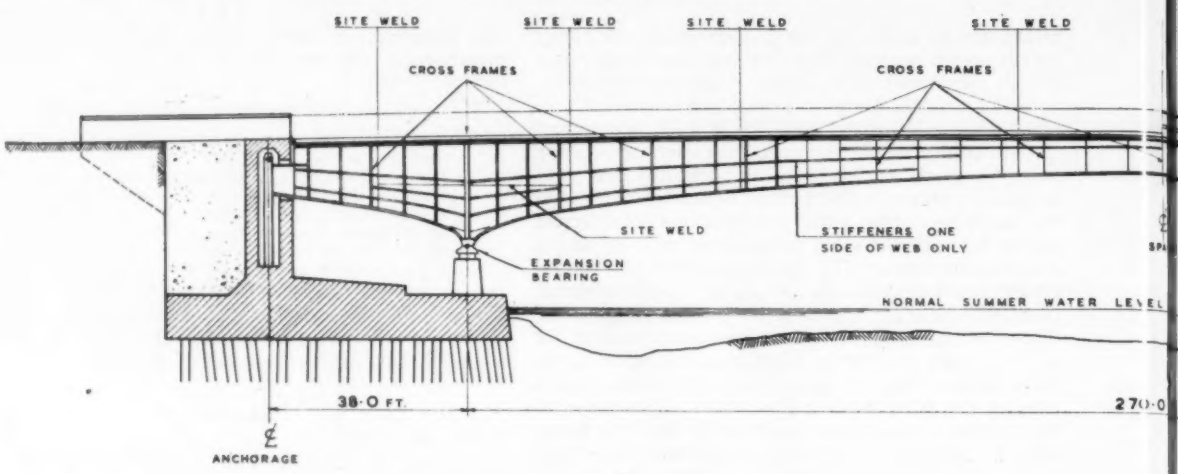
The use of concrete with a crushing strength of 4,500 lb/sq.in. at 28 days resulted in a reduction of thickness from the original 12 in. to $8\frac{1}{2}$ in. with a very considerable reduction in the dead load. The slab is keyed to the main girder flanges by properly designed shear connectors; in this case of T-section with hoops of $\frac{1}{2}$ in. dia. round welded to them. It may be noted that these appear to cost about the same as stud-welded shear connectors which might have been more handy to apply on the site. There was, however, some doubt as to the hardening effect produced by the stud welds on the high-tensile steel.

High-tensile steels

The quality of high-tensile steel most generally available for welded structures is that to BS.968, with a maximum carbon content of 0·23%. The rules for the satisfactory welding of this steel have been developed from the results of tests carried out for a committee of the B.W.R.A., and are summarised in BS.2642. Curves showing the minimum size of weld that may be applied to joints of various dimensions and 'thermal severity number' (TSN) have been developed by the author* for rutile electrodes. From these may be determined the minimum fillet size that must be applied in a single run to put sufficient heat into the joint to avoid cracking. The size of weld required is reduced if the material is preheated, and somewhat smaller sizes of weld are permissible if low-hydrogen electrodes are employed. The general consideration is that the size of weld to be applied in a single run should increase with the plate thickness, and in heavy construction it may become impracticable to apply a large enough weld for that quality of steel. This limitation is naturally most evident in manual welding and especially in site welding, since the size of deposit that can be made in the overhead position is strictly limited.

The guaranteed yield point of steel to BS.968 drops rapidly with increase in thickness. At a thickness greater than 1 in. it is only 19 tons/sq.in. compared with 23 tons/sq.in. for thin sections. For thicker sections, therefore, it may become economical to adopt a higher quality of steel in which the guaranteed yield point is maintained up to a thickness of 2 in. or

* G. ROBERTS: "Structural uses of Welded High-Tensile Steel". West of Scotland Iron and Steel Institute, May 1958.



2—Longitudinal section, new design

more, and which is more readily weldable. One steel of this quality is Coltuf 32, a low-alloy steel in which the tensile properties are obtained by controlled rolling and heat treatment, instead of by the addition of alloying elements. The tendency to cracking is therefore very much reduced and the fibrous nature of the steel gives it the useful property of resistance to crack propagation, or notch toughness. The yield point is 21 tons/sq.in. up to 2½ in. thickness.

In the revised design it was decided to use steel to BS.968 for the girder webs and flanges, except those flange plates in tension that had to be butt welded on site, for which Coltuf 32 was used. All stiffeners, bracing frames, and secondary members were of mild steel.

Design

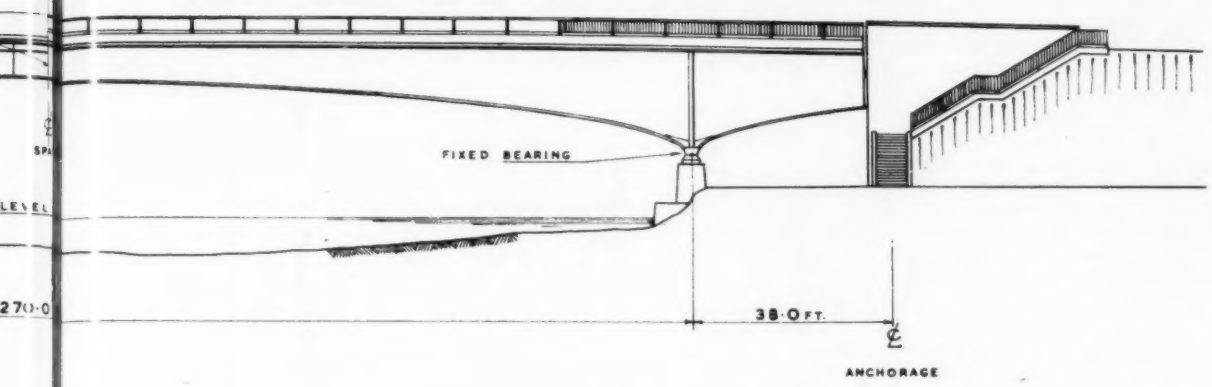
Design of girders

The working stresses used follow the requirements of BS.153 for the different classes of steel with their appropriate minimum yield points. The design of the web stiffening with horizontal as well as vertical stiffeners also follows the requirements of BS.153. At the deeper section of the girders several lines of horizontal stiffeners are required. The horizontal stiffening angles are inclined to follow the line of principal compressive stress in the web and terminate at a pressed steel channel-type of stiffener over the bearings. At its lower end this channel stiffener is widened out to the width of the bottom flange. The web plates are generally ⅜ in. thick in the shallower part of the girder and ½ in. elsewhere. Over the bearings, where the sharp curvature of the flange produces high compressive stress in the web, the web thickness is made 1 in. for a depth of 4 ft.

Where the stiffeners cross they are notched through or over each other and not cut into short lengths (*see* Fig. 4). This is an important point, in the author's opinion, as it makes more efficient use of the stiffening material, reduces the amount of welding, and tends to keep the web plates flat. The ends of the vertical stiffeners are butted against the flange plates and are connected by a 2½ in. long fillet weld on the outer leg. Where the bracing frames occur the vertical stiffener is stopped about 1 ft short of the flange. The web stiffening is entirely on the inner side so that the outer surface of the web is perfectly flush. Where there is a horizontal site joint in the girder the vertical stiffeners are butted in a ½ in. division plate 1½ in. below the web joint. In general, single unequal angles with a ratio of about 2:1 in their leg lengths form the most efficient stiffeners in welded construction, but where the steelwork is to be grit-blasted the outstanding leg should not be less than about 5 in. Flats are preferable to angles in the smaller sizes, and in this case 4 × ½ in. flats were used.

Near the bearings, the lower flange plates become quite sharply curved, considerably more so in fact than would normally be the case with a parabolic profile. The curvature of the flange produces a bending stress across its width which becomes excessive if the curvature is severe. In this case it was found necessary to stiffen the curved flange plates near the bearings by the addition of sloping flat plates, which also serve to prevent rainwater collecting at these points. The last and most sharply curved part of the ellipse is formed by shaping the bearing block itself and by the addition of a wedge-shaped making-up piece on the underside of the flange plate adjacent to the bearing block. The detail is shown in Fig. 5.

Once the weight of girder and the flange area required have been estimated by successive approximations, the next stage in the design is to determine the break-up of the girder into its various units; this determines the positions of the site joints in the flanges where changes in section can most conveniently be made. In addition there may be other shop joints for changes in sectional area, and the flange plates may also be tapered in plan if required by ripping a pair out of a single wider plate. In a continuous girder the bending moment varies rapidly along its length from positive to negative, and any attempt to make the flange section exactly fit the area required would involve an excessive number of joints. In designs of



2—Outside elevation, new design

this kind it is preferable to have too few rather than too many changes in flange thickness. This involves some sacrifice in total weight but provides more accurate and economical shop fabrication with a lower cost per ton, so that the total cost is not much affected. The length of the centre section is about the maximum that can be handled conveniently in most fabrication shops. The width of the top flange (which is smaller in area than the bottom flange owing to the interaction of the concrete deck) is made sufficient for lateral stability during handling and erection. Figure 6 shows the girder make-up finally adopted.

Bracing

In the final condition of the bridge, lateral stability is provided by the continuous reinforced concrete deck, made integral with the girders by the shear connectors. Stability of the bottom flange in the compression zone is provided by cross frames which, from the viewpoint of appearance, are preferable to continuous lattice bracing on the underside. Also for the sake of appearance it was thought desirable to keep the number of cross frames to the minimum and to make them as inconspicuous as possible. In addition to support of the bottom flange the cross frames serve the purpose of equalising the vertical live loads between the various girders. Under certain conditions of loading a large tension is developed in the bottom members of the cross frames, and this must be transferred through the girder webs at the end connections. For this purpose the most efficient connection, as well as the most convenient for erection, is one using high-strength grip bolts stressed to a tension of about 30 tons each. At this tension the friction developed between the end plate and the girder web is adequate to carry the vertical shear. The horizontal members of the cross frames as designed can be accurately made to the correct length and bevel so as to fit exactly between the girders. Figure 7 shows a typical bolted cross-frame connection. The deeper cross frames are designed for transport in two pieces, with a simple welded joint at the intersection of the diagonal members. The cross frames over the bearings were site welded directly to the vertical pressed channel stiffener, as shown in Fig. 8. Temporary angle cleats were used to locate the ends of the cross frames during erection.

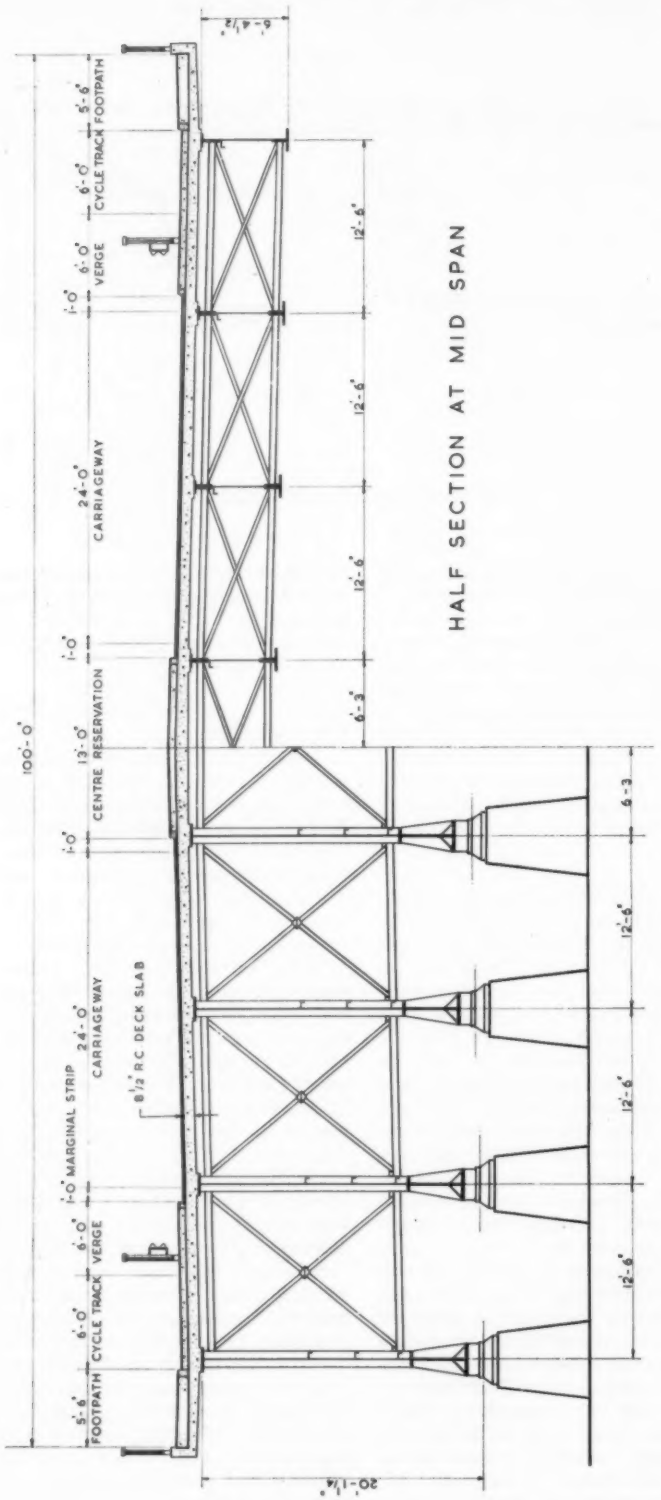
Fabrication and Erection

Details of welding

The ends of the flange plates were prepared for the site butt welds so that the centreline of the weld would be vertical. They are single-U welds with a $\frac{3}{8}$ in. root radius and 20° included angle. There is a $\frac{1}{4}$ in. root face and no gap, the root being chipped or gouged out for the overhead sealing weld (Fig. 9). The shop butt welds, where changes in flange section occur, were made with a double-V preparation before the flange plates were edge-planed (Fig. 10).

The fillet weld sizes were proportioned in accordance with the rules for welding steel to BS.968, using low-hydrogen electrodes. The web-to-flange fillets had leg lengths of $\frac{7}{16}$ and $\frac{3}{8}$ in. (laid in a single run) and therefore required the tilting of the girders to the gravity position. Manual welding was used for these fillet welds as nothing would have been gained by using an automatic machine. In fact, an automatic machine would have been difficult to use on the curved bottom flanges.

All the fillet welding of stiffeners to the web plates was intermittent, 4 in. miss 8 in. being generally employed. These intermittent welds were staggered or 'reeled' on each side of the stiffener. Intermittent welding is not favoured by some engineers, but in the author's opinion it is perfectly satisfactory and in fact to be preferred to continuous welding. It enables plates to be kept perfectly flat, especially if the stiffeners are notched and not cut short. This point is well illustrated by comparison of the Maidenhead web plates with those of a German bridge over the Rhine, illustrated in Fig. 12. In the latter bridge, which is otherwise an excellent example of welded construction, the fillet welds are continuous and the horizontal flat stiffeners are cut into short lengths.

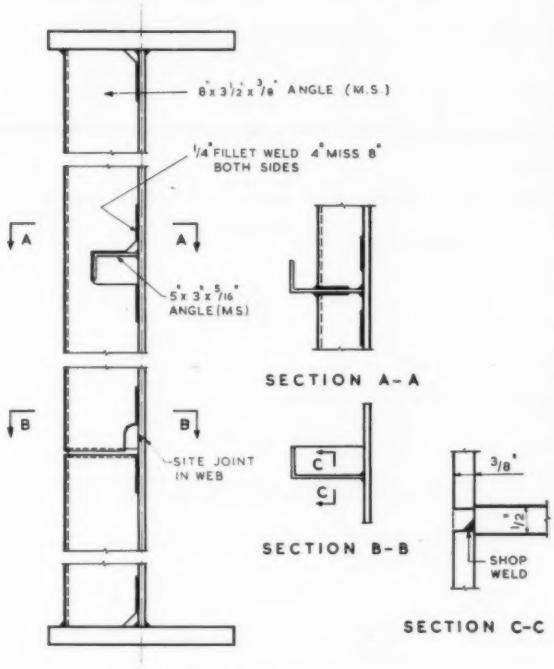


3—Cross section of new design

Erection

Since no interference with traffic on the river was permissible the bridge was erected by cantilevering the girders from both ends. Views of the steelwork during erection are shown in Figs. 13–16. The erection scheme was, in fact, very similar to that proposed by the author for the original design, but with the important difference that derricks of 10-ton capacity were used instead of the 20-ton cranes previously required. Lugs were temporarily welded to the ends of the flanges and longitudinal bolts were inserted through them to take the cantilever bending moment until the butt welds were completed. The bearings are fixed at one side of the river and rolling at the other. It was necessary, therefore, to lock the roller bearing in a pre-determined position, which was $\frac{3}{4}$ in. forward from its final location to allow for the spread of the bearings when the concrete deck was added. Before the centre closing lengths were erected the gap on each line of girders was carefully measured and the individual pieces were finished to the correct lengths before despatch from the shop.

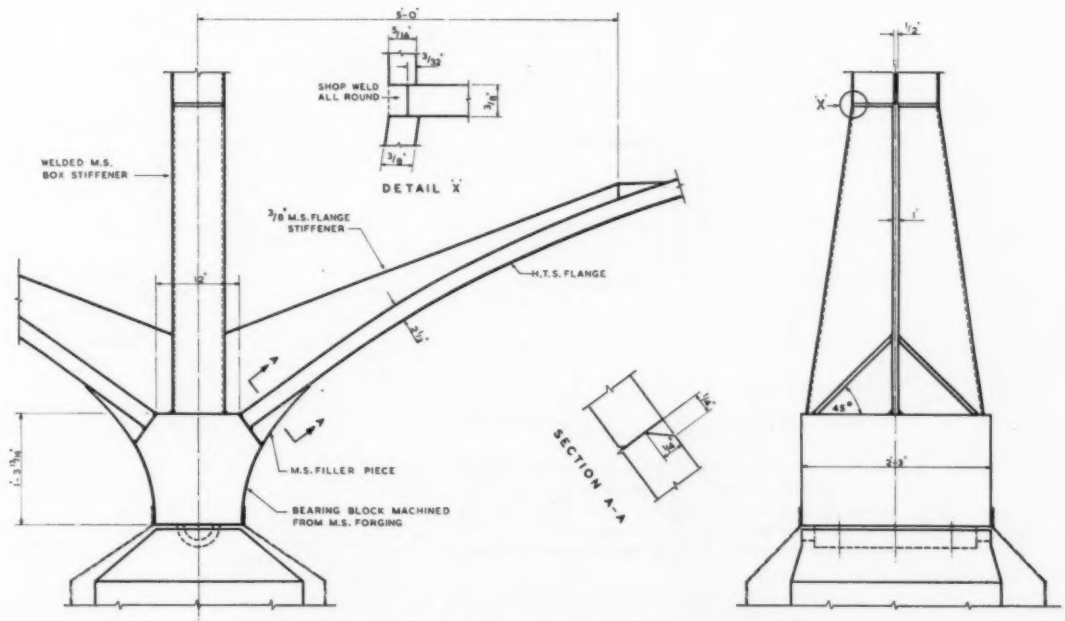
A very important feature of the erection scheme was the provision of temporary lateral bracing in the plane of the bottom flanges of the cantilevered girders, which otherwise would have been unstable without the concrete deck. This was effected with structural sections bolted to gusset plates, temporarily welded to the bottom flanges in such a way that they could be removed without damage to the permanent material. The contractors are to be congratulated on their ingenuity in the use of handy material for this purpose. The gusset plates were all accurately located in the shop and nothing was left to improvisation on the site.



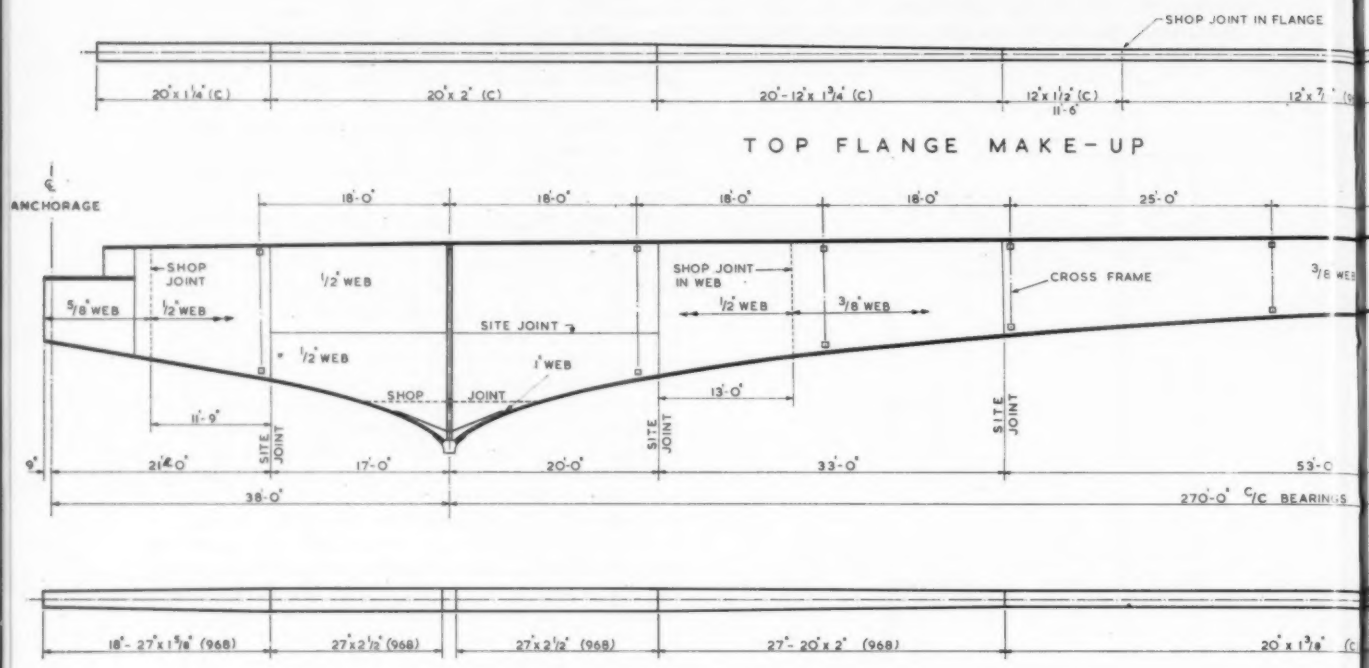
4—Typical details of web stiffeners

Site welding

The same types of electrode were used for the site welding as in the shop; $\frac{1}{4}$ in. for downhand welding and 10 s.w.g. for vertical and overhead welding. The flange butt welds were provided with extension pieces

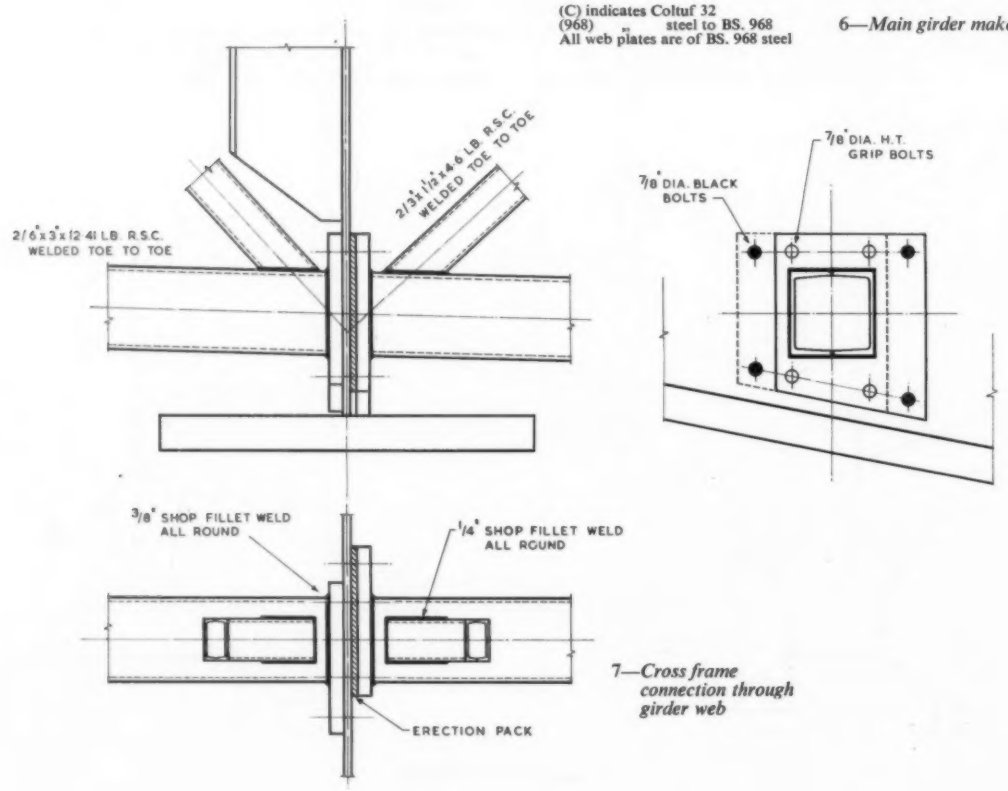


5—Detail at main bearing

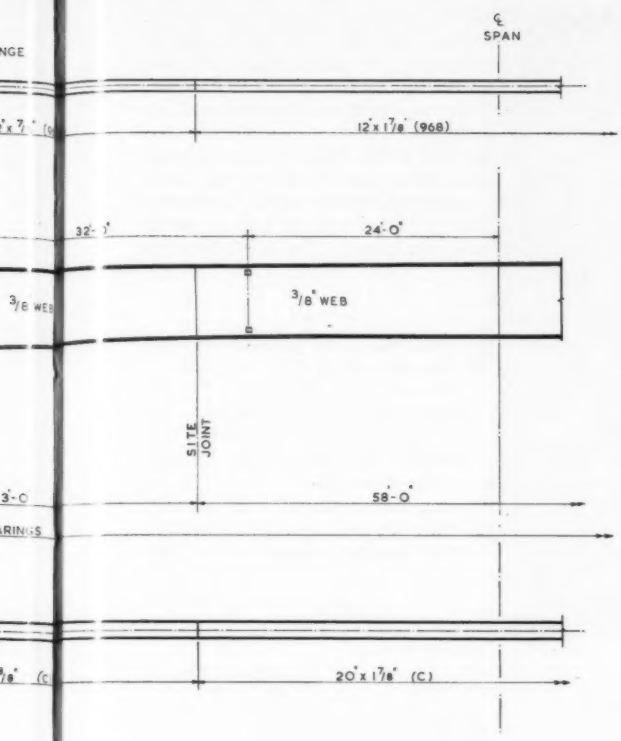


(C) indicates Coltuf 32
(968) " steel to BS. 968
All web plates are of BS. 968 steel

6—Main girder make-up


2/3"x 1 1/2"x 4 6 LB R.S.C. WELDED TOE TO TOE
2/6"x 3"x 12 41 LB R.S.C. WELDED TOE TO TOE
7/8" DIA. H.T. GRIP BOLTS
7/8" DIA. BLACK BOLTS
3/8" SHOP FILLET WELD ALL ROUND
1/4" SHOP FILLET WELD ALL ROUND
ERECTION PACK

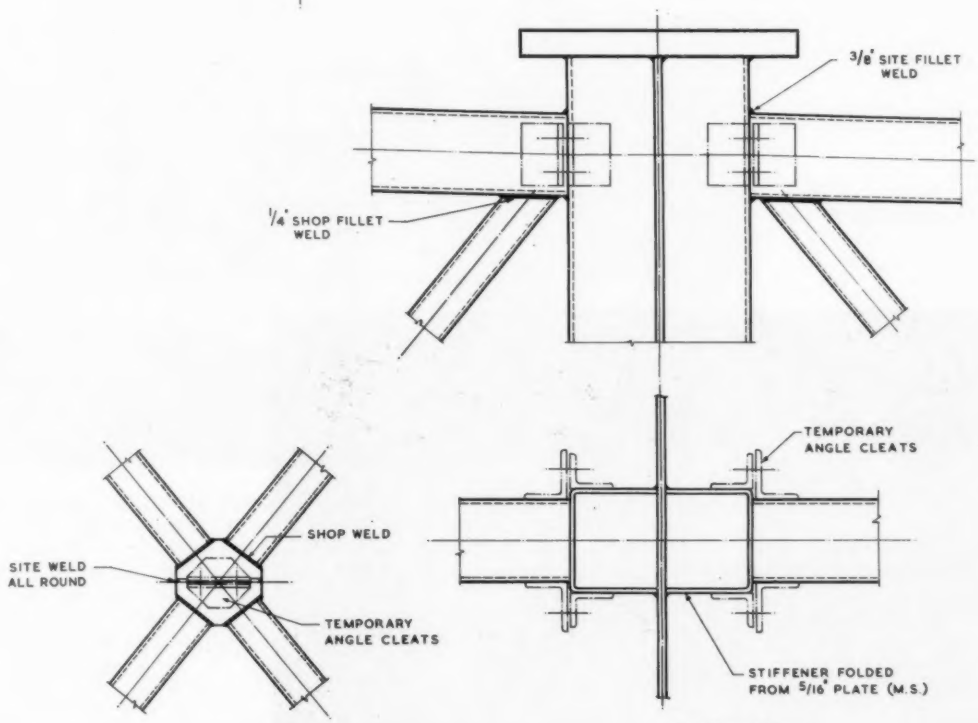
7—Cross frame connection through girder web



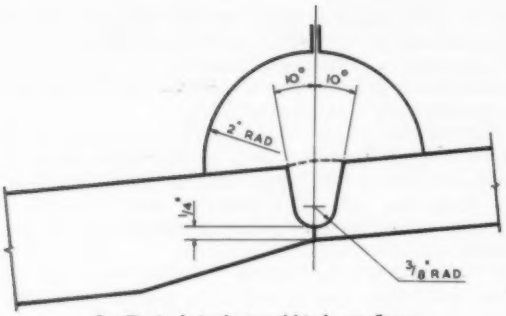
which were subsequently cut off, and the flanges chipped and ground by hand. Flame gouging was used for cutting out the root, and the sealing weld was made with several runs from 10 s.w.g. electrodes, approximately 5 in. per electrode.

On all the tension butt welds tapered edge covers were welded over the ends of the weld when the extension pieces had been removed. These might well be dispensed with if a machine were devised for removing the extension piece and grinding the edge of the flange perfectly flush. In the author's opinion hand grinding is not sufficiently reliable to avoid any possibility of notches or scratches, and the edge covers should be added to compensate for small defects of this kind. Figure 11 shows a typical edge cover.

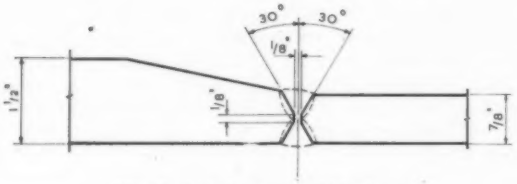
It was hoped to have each flange butt weld completed in one 'heat', including the sealing run, but this was not found practicable, and the sealing weld was done on the next day. The two flanges and vertical web joints were welded in one heat, however, using a pre-determined sequence, so as to avoid excessive compressive stresses in the web. Preheating to 'hand-warm' was carried out before starting to weld. In two of the joints in the BS.968 steel (compression joints in the structure) a slight degree of cracking occurred in the sealing weld. This was attributed to an unduly wide U gouged on the underside, and more especially to the overhead runs being too light. With a different technique of overhead welding no further cracking occurred.



8—*Cross frame connection over main bearing*



9—*Typical site butt weld in lower flange*



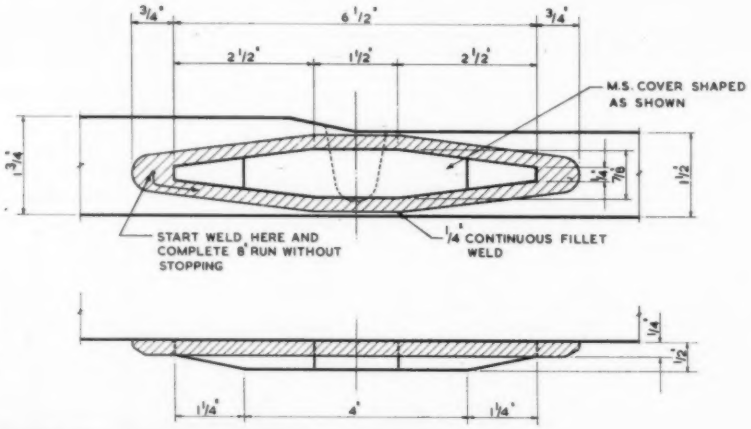
10—*Typical shop butt weld in flange*

Inspection

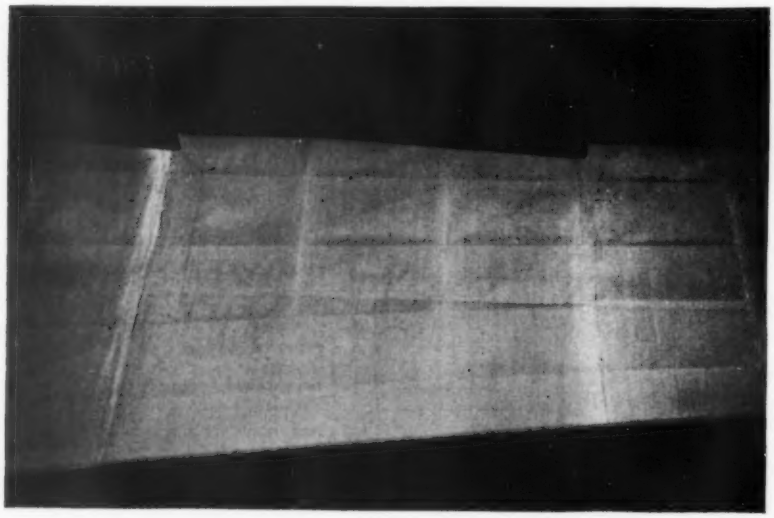
The contract specified that the contractor should provide X-ray equipment for radiographs of the important welds as directed by the engineer. In fact, all the butt welds were radiographed (though not for the full length of each weld), and no faults were disclosed in the tension butt welds requiring cutting-out and re-welding. Two faults were found in the overhead welding of the compression butt welds. A resident welding inspector was provided on site and all work in the shops was also examined. The steel was inspected at the mills.

Protective treatment

All steelwork was specified to be grit-blasted and sprayed with metallic zinc to a minimum thickness of 0·002 in. This was to be given one coat of etch primer (of a composition similar to that recommended by Dr. Jordan) and three coats of white lead paint. The steelwork was sand-blasted at the works and sprayed with a wire-type gun using $\frac{3}{16}$ in. dia. zinc wire. Some difficulty was experienced in obtaining a reasonably smooth surface on the zinc, owing to infused particles, but this was remedied by proper adjustment of the



11—*Typical edge cover for site butt weld*



12—*Outer web plate of German bridge*

13—*View of outer girder completely erected*

14—*View of girders during erection*

guns. In addition to the etch primer the first coat of white lead was applied in the works before despatch of the steelwork to the site. The remaining two coats, of slightly different shades, are being applied after erection, the finishing coat being a light blue-grey. It was found desirable to wash the site welds with water to remove the caustic deposit produced by the electrode fumes.

Economics

Weights of alternative designs

The following comparison of the weights of steel in various alternative designs is given as a matter of interest. In the original design, entirely in mild steel and largely of riveted construction, excluding bearings and footway parapets, there were 1,525 tons. The corresponding weight of steelwork in the present design is 620 tons. This startling reduction is attributable partly to the reduction in dead load of concrete and the design of the girders for full composite action with the deck slab. The rest of the reduction is due to the use of single-web instead of double-web girders, high-tensile instead of mild steel, and welding instead of riveting.

The reductions in weight brought about by each factor separately are approximately as follows:

15—*View during erection, showing stiffeners on inner side of webs*

16—*View from river during erection*

(*a*) Reduction in dead load and composite design	18%	(0·82)
(*b*) Single web instead of double web girders	17%	(0·83)
(*c*) High-tensile steel instead of mild steel	20%	(0·80)
(*d*) Welding instead of riveting	24%	(0·76)

The original weight of 1,525 tons is therefore reduced to:

$$1525 \times 0{\cdot}82 \times 0{\cdot}83 \times 0{\cdot}80 \times 0{\cdot}76 = 620 \text{ tons}$$

Time and cost

The contract for the superstructure was awarded to Messrs. Horseley Bridge & Thomas Piggott Ltd. in May 1959. The erection of steelwork on the site commenced on 1st February 1960, and was completed on 29th July 1960. The concrete deck was sub-let to Messrs. Higgs & Hill, who commenced concreting on 5th October, 1960 and completed the work on 22nd December, 1960.

The total contract price was £196,000. The actual total cost is approximately as follows:

All steelwork, including erection and painting (approx. 680 tons)	£125,000
Reinforced concrete deck, etc.	45,000
Handrailing and sundry items	10,000
TOTAL	£180,000

At the time of writing the contract is substantially completed and only the handrailing and sundry items, such as roadwork and new surfacing to the concrete abutments, remain to be done.

Acknowledgments

The author wishes to make acknowledgments to: The Ministry of Transport, in particular the Assistant Chief Engineer (Bridges), Mr. H. C. Adams, M.C., M.I.C.E.; his partner Mr. O. A. Kerensky, B.Sc.,

M.I.C.E.; the staff of Freeman, Fox and Partners, especially Mr. Michael Field, B.Sc.(Eng.), and the Resident Engineer on the site Mr. Kenneth Goodearl, B.Sc.(Eng.); Mr. R. J. Fowler, Chief Engineer of the Horseley Bridge Company and their very competent erection staff.

Discussion

Mr. R. J. Fowler: I propose to confine my remarks to certain erection aspects of the welding and to ask my colleague, Mr. John Allen, who was responsible for engineering both the shop and the site welding, to speak on that in detail.

When my company received the enquiry to tender, our first major preoccupation was with erection problems. We were, by specification, permitted to utilise some system of temporary supports from the bed of the river, but a navigable opening 20 ft high by 45 ft wide had to be provided, near the tow-path side. We decided that to cantilever was the cleanest and most economical method of erection, but many deflection calculations were necessary to show whether we would, in fact, end up with the extreme cantilever noses correctly levelled to ensure the correct profile of the bridge when the centre units were in position.

A further important problem to be solved was how to control (for it cannot be prevented) the contraction of the site butt welds to prevent a rise or fall in each cantilever nose as they were welded, due to unequal contraction of top and bottom flange butts, which at most joints were of unequal cross-section.

In addition to carefully planned welding procedures we took advantage of the stiffness of the cross-bracing. Each set of eight shop fabricated cantilever units was erected complete with service bolts and cleats, and the cross frames were service black bolted. Only then was welding permitted to commence on any of these eight girders. Hence, while any joint was being made, the neighbouring girders, via the cross-bracing, helped to maintain the position of the girder being welded.

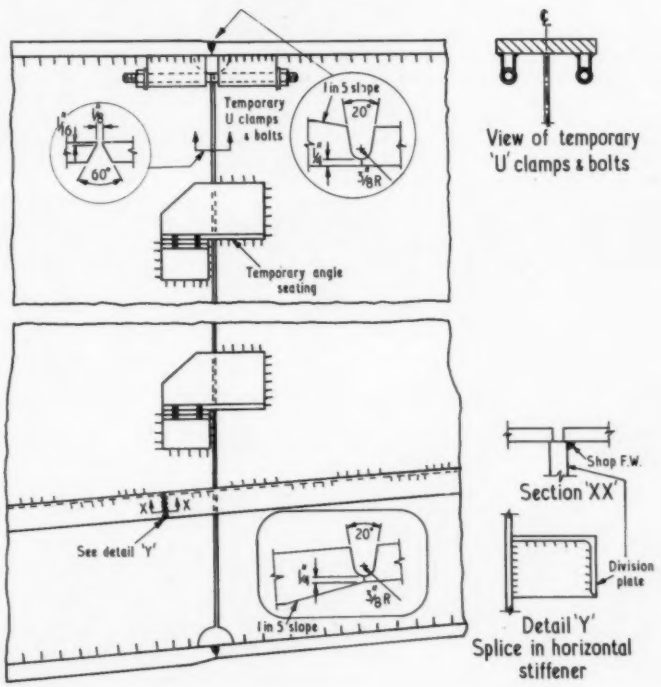
In considering lateral stability, both during erection and until the concrete deck was placed, we decided that improvised guying would be quite unsatisfactory. We provide two systems of bracing in the plane of the bottom flange, one between girders 2 and 4 and the other between girders 5 and 7 in plan, and all detailed just as though it could form a permanent part of the bridge. The maximum wind load at 15 lb/sq.ft pressure was of the order of 60 tons. The reaction to these horizontal girders was provided by the sides of the openings in the abutment faces against which the girders were securely packed, plus the resistance offered by the bearings in horizontal shear.

Each angle bracing in the system was connected to its gusset plates with two bolts and sufficient fillet weld to transmit the load; the bolts were purely for location, not for strength. The angle bracing was fillet welded to the gusset, which was already shop welded to the girder. This horizontal bracing also squared up the four cantilever girders automatically as soon as the bolts were inserted.

Incidentally, all these temporary bracing angles were from our stock of BS.548 high-tensile transmission line tower steel. We had no welding troubles with it and all was returned safely to stock.

We gave much thought to the type of service connections for locating and supporting the cantilever girder sections and also the main crossframes over bearings and the half blocks at the intersections of the crossframes. This is a complex problem in a welded plate structure where erection stresses can be appreciable.

Mr. Roberts will remember that we suggested the use of service bolts in drilled holes in the webs, to which we could connect with temporary cover plates, etc., and which holes we could fill in either with weld metal or with punchings welded in and ground flush. This we were not permitted to



A—*Typical site joint*

do. Eventually, we decided to weld cleats of the required shapes and forms to both webs and flanges and to ensure that after use they were very carefully removed and ground flush. Since there were 64 full-depth butt welds in the girders and 16 horizontal web joints over the bearings, each nearly 40 ft long, it will be appreciated that the accurate positioning of all the necessary cleats, their welding and their ultimate removal was an important part of the cost of the job.

Figure A shows the main service cleats for the erection of the individual girder units. Shear was taken by the cleats, two angle cleats being welded to each girder, with bolts between and appropriate packings to bring the girders to the correct relative levels. The bending moment due to the cantilever arm was taken by the bolts through the U-lugs under the top flanges of the girders.

These lugs with their longitudinal bolts were designed to withstand forces up to 11 tons each, and served the important purpose of providing adjustment for level of the cantilevers. This system depended very much on the accuracy of fabrication of the girder sections, and its success was such that we would not hesitate to use it again in similar circumstances.

We considered it desirable to make a practical test of the strength of the lugs, so we connected two opposing long cantilever sections together in the works. These were then supported at the centre, and so that each cantilevered 53 ft and loaded the bolts 1·35 times more than the maximum erection load. This load, and a little shaking of the girders, satisfied us that the bolts would be adequate for all conditions that they were likely to meet on site.

The U-lugs were generally 8 in. long and welded with 16 in. of $\frac{3}{8}$ in. high-tensile fillet weld. They were located on the underside of the flanges to avoid obstruction to the welders in filling the grooves of the butt welds. The bolts were removed before welding the overhead sealing runs, and were $1\frac{1}{4}$ in. dia. *R* quality (45–55T) material screwed both ends.

Both shop and site radiography were carried out on the butt welds in webs and flanges in plate thicknesses from $\frac{3}{8}$ in. to 2 in., using portable X-ray equipment for the thin and medium thicknesses. A caesium 137, 5-curie gamma bomb was used for the 2 in. thick butt welds, and exposures were made outside working hours, either in the early morning or late evening.

Bearing in mind the total number of site butt welds—128 flange joints and some 1200 ft of web welds—the small number of faults reported by Mr. Roberts denotes, I think, a high standard of welding of which all concerned can be very proud.

Steel bridge and other fabricators generally are supposed to be skilled and expert in the application of protective treatments, whatever type they may be. We deemed it desirable to consult the Corrosion Advisory Service of BISRA for guidance in our methods of application, etc., and we also made a series of control specimens in parallel with the application on the job. These are exposed on the roof of our drawing office at Tipton in the corrosive Black Country atmosphere and should give an early warning of any breakdown. So far the specimens show no detectable trouble.

The specimens have all been shot blasted, metal sprayed, etch primed and painted, and then two further coats of paint have been applied as on site in sequence with the actual job.

We ordered about 750 tons of steel to make the bridge and sent to the site about 725 tons, which included some of the cross-bracings. I do not think that in his figure of 620 tons Mr. Roberts included the expansion units at the end of the bridge. He has not mentioned these units in the paper, but I consider that they were the most difficult parts of the bridge to make. The weight calculated by the consulting engineers was some 680 tons.

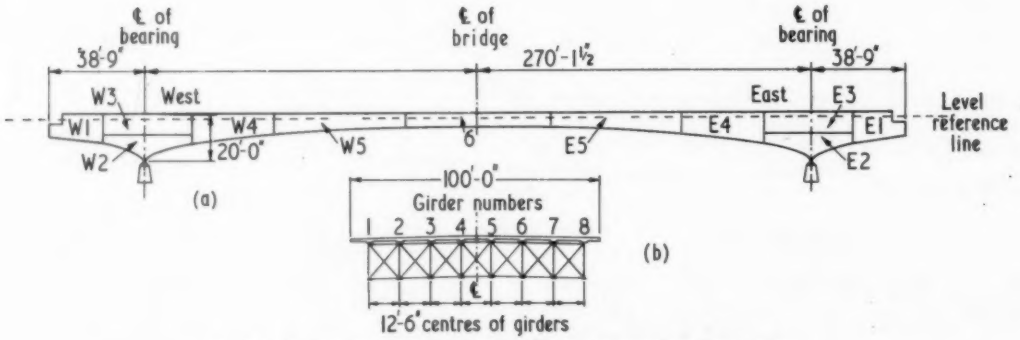
The contract was placed in May 1959. Steel deliveries to the site commenced at the end of January 1960; completion of erection and welding of steelwork, end of August 1960; access for concrete and roadwork subcontractor, beginning of August 1960. The original contract date for completion was 18th December, 1960, and the erection of the steelwork went almost exactly to plan and programme.

Mr. J. S. Allen: Figure B indicates the sub-division of the girders as fabricated.

With normal welded girder work with parallel flanges it is good practice to fabricate the girder sections long and then to cut and mill them to exact length. Owing to the shape of the bridge girders—they had a rapidly changing depth—it was not possible for that practice to be adopted on this contract, and another system had to be devised for all but the centrepieces No. 6, which had quite a small change in depth. These were kept in the works until final closing dimensions could be obtained from site, and were then milled to length.

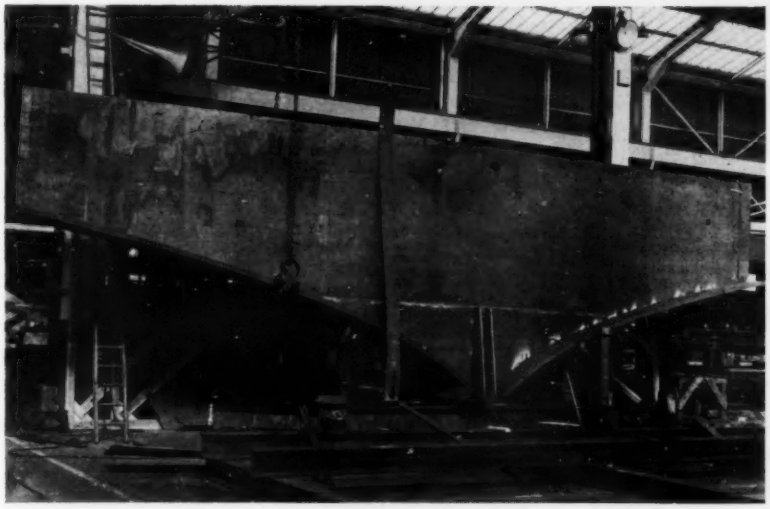
The system adopted was to calculate the expected amount of shrinkage due to shop welding and site welding, and each section was fabricated long by that amount exactly. During the shop welding the expected contractions took place and it was thus possible to obtain the extreme accuracy required in the girder sections to permit the successful erection of the members by that cantilever method adopted.

The shop fabrication and welding procedures for the girder sections were carefully planned before work commenced, and procedure drawings were made controlling the fabrication and welding of each section.

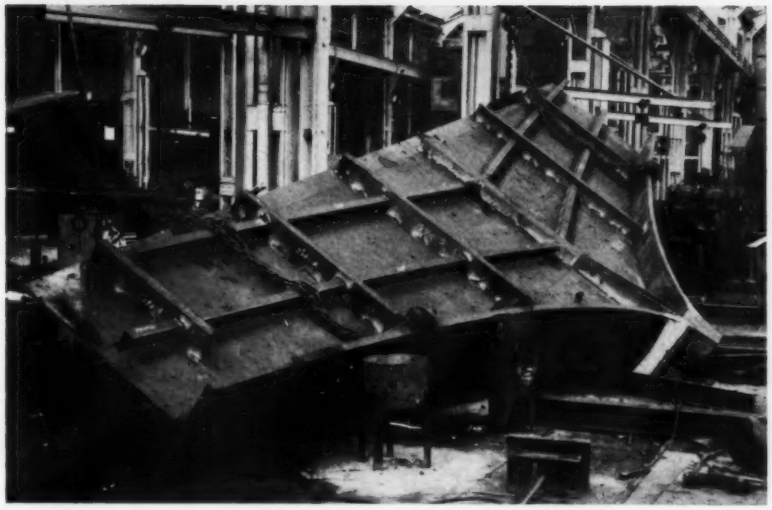


B—(a) *Subdivision and marking of girders;* (b) *section through bridge*

C—*Layout of first girder web*



D—*Vertical assembly of Section 2 girder*



E—*Position for flange–web welding*

F—*Shop assembly of girders for checking*

For the first girder, each section of web plate was cut and prepared with its end preparation to the drawing dimension plus the shrinkage allowance. The web was laid out as in Fig. C, and the curved top and bottom edges were marked and cut. These web plates were used as master templates for the marking of the remainder of the plates on the other girders.

The girder sections were then fabricated and welded to the specified procedure. The stiffener welds were completed first, followed by the welding of the web to the flanges. The welds to the bottom flange were made before those to the top flange to assist in maintaining the camber in the girders.

To obviate the necessity for preheating, the main web-to-flange welds were made in the inclined position so that the correct size of weld could be laid in one run, this in some cases being up to $\frac{3}{8}$ in. leg length.

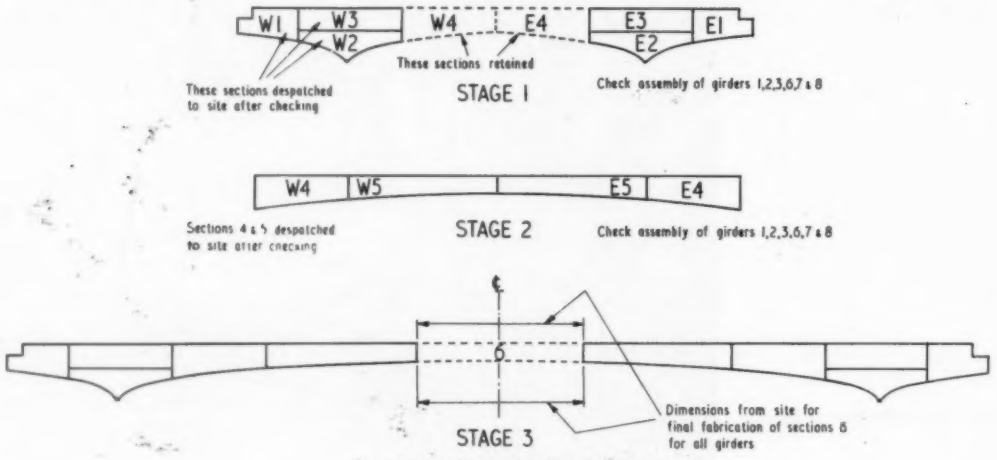
Sections No. 2, which were over the bearings, were the most complex to fabricate and weld. Firstly, the shop joints were made in the web plates, and the curved edges cut. Assembly took place in the vertical position and a special frame was used to ensure that the flanges were in close contact with the web. This is shown in Fig. D.

Figure E shows a Section 2 being welded. As the main web/flange welds were made in one run, theoretically there was no need to preheat, but as these sections had the heaviest flanges on the contract some pre-warming by the use of coke braziers was used.

Figure F shows the shop assembly and checking of the girder sections. The first two girders to be fabricated were laid down on the shop floor complete, with the exception of the central closing sections. At this stage all the horizontal joints and the straight-through site joints were carefully checked.

The system used on the remaining six girders is shown in Fig. G. Sections 1, 2, 3, and 4 for the east and west sides, were laid down and checked. Pieces 1, 2, and 3 on each side were then despatched to site, pieces 4 being kept in the shops. When Sections 5 had been fabricated they were laid down and checked with Sections 4. Finally, Sections 6 were checked from site dimensions and cut to exact length. Thus each section of every girder was carefully checked before despatch to site, and satisfactory erection of the girders was ensured.

Mr. Roberts has noted that it was not found possible to complete the whole of the welding of the site joints in one



G—*Stages of assembly for checking*

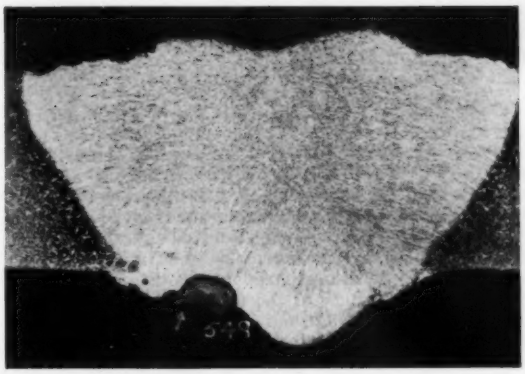
operation. This was never intended, for it was appreciated that the back chipping of the butt welds would take time.

The site welding was very carefully thought out and controlled. At any vertical joint welding commenced on the flanges and the welding grooves were partially filled, employing one welder on the top flange and two on the bottom. After a given number of runs had been made the welders stopped work on the flanges and two of them transferred to the webs, where the face welds were completed. The three welders then returned to the flanges and completed the flange welding. To this stage the welding was carried out in one continuous operation, the seal welding of the joints being carried out later as a second continuous operation. Figure H shows a typical section through one of the flange welds.

The butt welds were initially back chipped, but the procedure was rather slow. We therefore resorted to flame gouging, but each gouge was finished with a disc grinding wheel mounted on a high-frequency machine. Should there be any small faults still remaining in the root we believe by this system that it is possible to see them.

In regard to back chipping, Mr. Roberts had suggested that it might be possible to use a carbon backing bar for the welds and to use an electrode that would give sufficient penetration to go right through the root and form a bead on to the carbon backing bar. Figure J is a section through a test in BS.968 material welded on to a carbon backing bar with the suggested electrodes. It will be noted that there is carbon pick-up, with associated micro-cracks. In this area there is also excessive hardening (549 D.P.N.). It seems, therefore, that the flame-gouging procedure, whilst a little more laborious, is perhaps more satisfactory.

Mr. Roberts has referred to the use of edge covers. We carried out tests to determine their effectiveness because we felt that possibly they might have some detrimental effect on the strength of the joints. A series of specimens in BS.968 steel were made with different edge finishes, including the worst kind of ground finish with the grinding lines going across the edges of the plate; a smoothly ground surface made with a high-speed disc girder; a rough machined finish; and a specimen smoothly ground with edge covers welded on. All the specimens failed in a ductile manner with the exception of one specimen which had edge covers welded to it. A further four specimens were therefore prepared to which edge covers were welded and, of these, only one failed in a ductile manner. The others failed in a brittle manner across the extreme ends of the edge covers as shown in Fig. K.



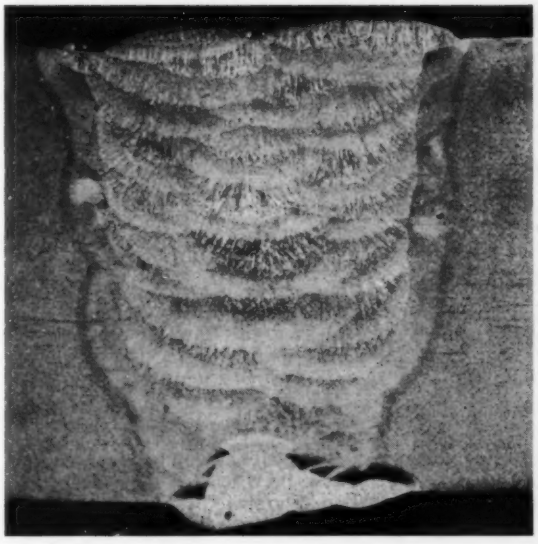
J—*Section through test weld made on carbon backing bar*

It is appreciated that the specimens are relatively small compared with the large flanges on the bridge, but perhaps there is some indication of possible danger here.

Mr. O. Bondy: Certain refinements in design seem to distinguish this latest welded bridge from quite a few others, and I have no doubt that these refinements must have contributed to the high cost per ton. I only hope that the total cost proved to be absolutely economical. After all, cost per ton of steelwork is only one index, and it is not necessarily the one that would decide the economy of the completed bridge.

Three different types of steel were used in this bridge. Would not this have been a major source of extra cost?

The cross-bracing between the individual main girders was arranged also with a view to improving appearance.



H—*Typical section through flange weld*

K—*Brittle failure of specimen with edge covers*

Would not this have been a typical case for using tubular members? According to a very rough estimate that I have made, the horizontal chords alone needed more than 5000 ft of shop welding, which could have been dispensed with by the use of tubular sections.

Mr. G. M. Boyd: I am very intrigued by the impunity with which these bridge people can ignore the phenomenon of brittle fracture. There are one or two passing references to it in the paper, but I see that even the Coltuf 32 steel was chosen not for its toughness, which is regarded as incidental, but for its guaranteed yield point.

One or two references have been made to this phenomenon in the course of the discussion, particularly by Mr. Allen, and I should like to ask him whether his test pieces were made in summer or winter. The time of day and the temperature at which they were made could affect the results.

Mr. Allen: They were made in summer.

Mr. Boyd: In shipbuilding, we have found to our cost that we cannot afford to ignore brittle fracture, and more and more branches of engineering are also finding this to be the case. Bridge constructors have not yet apparently experienced enough of this trouble to warrant its being considered very seriously in the design of a bridge of this kind. I should like to know to what they attribute their 'longevity' as it were.

I have gathered quite a number of cases of brittle fracture failures in bridges, not only the Hasslet Bridge in Belgium but the German Tiergarten Bridge and the Canadian Duplessis Bridge, to mention only a few. I wonder whether these affected the considerations and the choice of material, and, particularly, whether any tests were carried out on the steel to study this point.

Although I like the clean lines and design of the bridge there are one or two things that I would have queried. For example, there is the notch on the stiffeners. Notches are just anathema to me.

A more serious point is the way in which a large number of small shear cleats were welded to the top flange. On the basis of the experience that one has gained in other spheres, that would seem to be rather a bold undertaking. Was any special care taken over the welding of these cleats, for so far as the stresses are concerned this is just as important as the welding of larger items.

In regard to flame gouging, I should have thought that it would be difficult to get a controlled treatment in the overhead position at arm's length, but in the downhand or horizontal position with proper modern techniques and usual skill it should be satisfactory, particularly if the gouge is ground off afterwards.

Mr. O. A. Kerensky: As a co-designer of the bridge with the author, perhaps I might make a few comments to illustrate some of the points of design.

The bridge has a flat reinforced concrete deck, so that in future provision of six instead of four traffic lanes would be possible. Because the superstructure consists of multiple girders such additional loading can be carried with maximum efficiency involving practically no extra weight. The H.B. loading placed anywhere on the bridge was distributed to all the girders, and the deck and cross bracing designed accordingly.

The saving in weight has been mentioned. I would stress the economy from combined action design. In particular we provided shear connectors in the tension zones, thus utilising the whole of the steel reinforcement of the concrete slab in the flange area of the main girders for live load stresses.

To avoid excessive cracking, the sequence of casting of the concrete deck was specified, the concrete over the supports being cast last to eliminate tensile stresses due to dead load. The subsequent effects of live load are relatively small. To prevent any possible seepage of water through the deck slab, it was waterproofed with two layers of mastic asphalt, before surfacing with $1\frac{1}{2}$ in. of hot rolled asphalt.

I would query Mr. Bondy's statement that the bridge is expensive. The figure of £180 per ton metallised and painted compares very favourably with today's prices. It should also be remembered that the weight of the bridge has been reduced from 1600 tons pre-war, to 600 tons. I believe it to be one of the cheapest bridges of comparable size in the country.

Mr. Roberts said that he likes Coltuf because of its high strength. I like it, too, especially as it also happens to be notch ductile. One may say that it was chosen for these two reasons.

Finally, I should like the author to give comparisons between the costs of Coltuf and notch ductile mild steel, because I cannot understand anyone using notch ductile mild steel at all, and believe that suitable high-tensile steel should always be more economical.

Mr. Beyner: What is the maximum tension in the bottom flange that the bridge is likely to receive?

Mr. Capper: I should like to know the composition or tensile strength of the various steels that have been mentioned. We know Coltuf, but I am not familiar with the British Standard specifications for some of the others.

Mr. Freeman: The radiographing of welds has been referred to. Could Mr. Roberts make some comment on the interpretation of the films as a result of which welds were considered acceptable or rejected?

Author's Reply

Mr. Gilbert Roberts: First of all, I should like to congratulate Mr. Fowler upon the very painstaking way in which he has studied all the problems with which he was confronted when his firm took on the contract. They were problems of erection, procedure, fabrication and so on, and the all-important item of the design of the temporary work—that is, the temporary bracing and supports—a feature of these bridge schemes which can be treated lightly only at the great peril of the contractor, the consulting engineer, and everybody else.

To undertake a welded bridge for a major crossing like this over the Thames in the limelight of publicity, with site welding and high-tensile steel and an advanced type of design obviously raises many problems, but with a resolute attack on these problems, such as was provided by the band of enthusiasts who designed and built this bridge, it is bound to end in success.

I think you have been very fortunate to hear Mr. Allen's lucid description of his procedure of fabrication in the shop, because that is one of the features of bridge work—in fact, of all structural work—which, in my opinion, is sadly neglected. In most institutions you will find many engineers who will proudly describe the moving of derrick cranes and the weights of lifts and so on, but they completely neglect the fact that if the steelwork had not been properly made for the job they could not have erected it at all. It is only right that the problems of shop fabrication should be given their credit in these papers.

Mr. Bondy mentioned the expensive special steels. The utility quality high-tensile steel to BS.968, which is the welding type, is not very much more expensive than mild

steel—about 10–15%, in cost—and that was the steel that we mainly used. As I have explained, the difficulty in welding it became apparent only at thicknesses over $\frac{3}{4}$ in. or perhaps 1 in., and it became almost insuperable at thicknesses of $2\frac{1}{2}$–3 in. We used the more expensive Ducol steel only for those parts of the structure where it was necessary, and, therefore, reduced to the minimum the additional cost in material on account of quality.

The cross-frames are virtually made of tubes, the tubes being made with two channels welded toe to toe. If square tubes of the requisite thickness had been available at that time, we should have used them.

Mr. Boyd seems to object to my doing the right thing for the wrong reason. He is in favour of my using Coltuf steel, but I ought to use it because I am afraid of brittle fracture and for no other reason. I can appreciate that he wakes up in the middle of the night and thinks of the thing going off with a 'ping' and with a nasty crystalline look when it has broken. I must admit that we structural engineers have not had the wide experience of brittle fracture that they have in shipbuilding, but we think about it nevertheless. If he reflects that all the tension butt welds in the bridge are in notch-ductile steel, I think he will feel less apprehensive, even though we do not use notch-ductile steel for the reason that he would like.

There is also the question of edge covers, at which Mr. Allen was having a tilt. I must admit that his tests showed that no improvement was gained by their use. On the other hand, there is no serious diminution in strength, whereas by leaving the ends of butt welds as-welded or roughly finished you can, I know, get the start of a crack. We must remember that in many of these cases we are dealing not with notch-ductile steel but with the ordinary run-of-the-mill steel that we are accustomed to use and which is quite satisfactory if properly used. It is a matter of being sure that the edges of these welds are adequately finished. I mentioned in the paper that if some machine could be devised to end them consistently I for one would be quite prepared to leave the edge covers off.

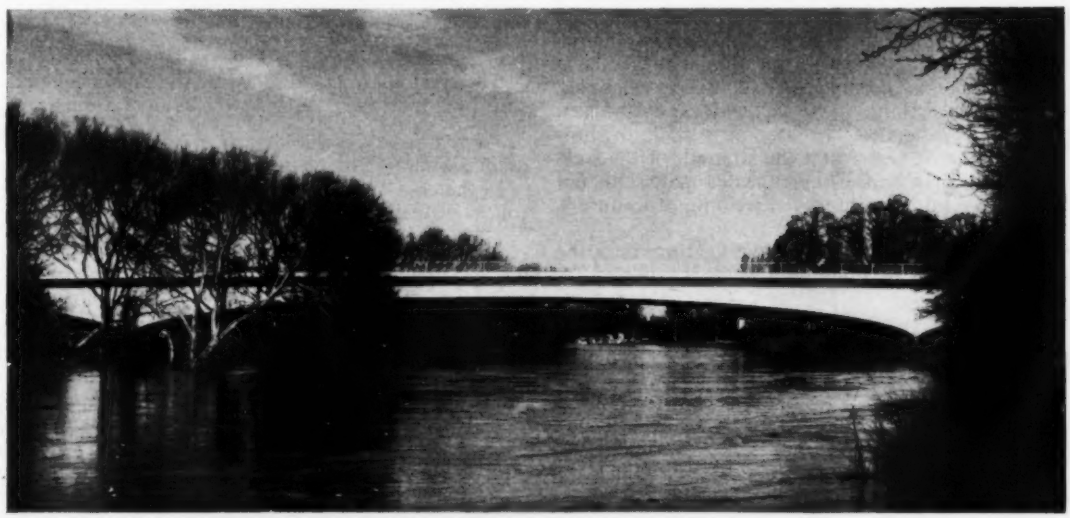
It is just conceivable that our attention to little details like edge covers, which may not seem of the same importance in shipbuilding, is the reason why we have not had the same experience of brittle fracture in structures as has been found in ships.

Mr. Boyd mentioned that the welded shear connectors on the tension flange were undesirable. The welding procedure for the welding of these T-sections was laid down and rigidly adhered to. They are at the centre of a wide flange, not on the edge, and the steel is Coltuf. In my opinion, no risk is involved.

The tension design stress in Coltuf is $12\frac{1}{2}$ tons/sq.in., compared with 9 tons/sq.in. for mild steel. The composition of our mild steel of standard quality (28–33 tons/sq.in. tensile strength) is about 0·2% C and 0·5% Mn and the BS.968 material is 0·23% max. and 1·2–1·6% Mn. The Coltuf steel has about 0·18% C and I suppose 1·2–1·4% Mn; but its special quality derives from controlled rolling and heat-treatment. Its toughness is the result of physical treatment rather than chemical constituents.

Mr. Freeman made rather a good rapier thrust, as it were, in asking how we interpreted the radiographs. It is always difficult to decide just how bad a radiograph has got to be before you have to condemn the weld. We adopted the principle that there should be no cracks of any sort and no more slag inclusions, blow-holes, and so on than would be represented by the stars on a bright night in spring. I think that that is as good an explanation as I can give.

Mr. R. G. Braithwaite proposed a vote of thanks to the author, and referred to the great experience of Mr. Roberts —both in design and in welded fabrication.



View from the river of the completed welded Maidenhead Bridge

UTRECHT MEETING, 1961

Some Remarks on the Weldability of Aluminium

By G. Westendorp

Although aluminium is very successfully welded there are still several unsolved problems, particularly when results are compared with those obtained with other metals.

One of these problems is the reduction in strength caused by the welding heat. A second is the porosity which is found in many aluminium welds. These problems are discussed in the paper.

WELDING of aluminium has made considerable progress. In many fields, *e.g.*, shipbuilding, the chemical industry, railroad stock and other transportation equipment and even nuclear plants, the welding of aluminium is a common practice and is sometimes a highly developed art.

However, if the results are compared with the properties of welds in other metals, the aluminium weld does not yet have an equivalent quality. Some of the problems met with in aluminium welding are considered as inherent to the properties of the material and are therefore unavoidable. The corollary of such a statement can only be that the field of application of welded aluminium will be limited. It is therefore necessary to proceed with our development work until the weld in aluminium is as reliable as the material itself.

In this paper, two problems are chosen as examples. Firstly, the weakening of the welded joint by the heat of welding is discussed. An attempt is made to show that the common belief that the strength of the weld is no greater than that of the annealed material is not necessarily valid if an improved welding procedure is used.

Secondly, the start of an investigation into the causes of porosity in aluminium welds is reported.

Strength of the Welded Joint

The decrease of strength in the heat-affected zone is caused by a number of changes in the crystal structure of the alloy. These changes are different for each type of alloy.

Pure aluminium

In the case of very pure aluminium the structure contains only aluminium crystals, normally cold-deformed in the last manufacturing stages of the material.

Starting from a point where the properties are not changed by the welding heat, and going in the direction of the weld, one meets the following changes in structure and properties:

(*a*) *Recovery*

In the first zone, the microstructure is not changed visibly, but the strength is decreased by the phenomenon known as 'recovery'. The strength will have a value between that of the cold-deformed material and that of the annealed material.

(*b*) *Recrystallisation*

In the second zone, the metal will be recrystallised to a grain size which is dependent on the amount of deformation in the plate material and on the heating cycle. The strength in this zone is decreased to the strength of annealed aluminium.

(*c*) *Crystal growth*

The crystal size is increased by the higher temperature and the longer time at the temperature during welding.

(*d*) *Fused material*

In the case of pure aluminium, the fusion zone will follow directly upon the zone of crystal growth.

Commercially pure aluminium

For commercially pure aluminium, *e.g.*, 99·5% Al, the same structures are found with some complications, because the structure of the material contains several other phases as a result of the presence of small amounts of Fe and Si. Without too great a simplification we can use a two-phase model for this structure, in which the second phase has an increased solubility at higher temperatures and forms a eutectic with the Al crystals.

On the basis of this simplification, the influence of the second phase will be negligible in the zone where recovery is the only change; in the second zone, where the Al is recrystallised, the second phase will partly

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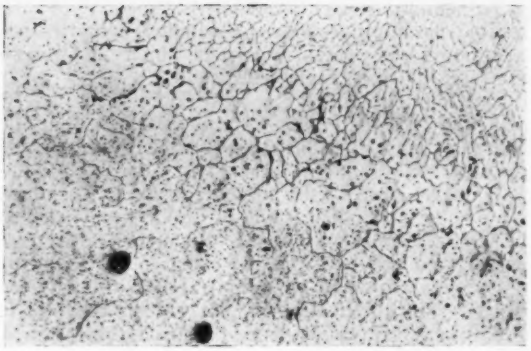


1—*Structure of transition zone in 99·5% pure aluminium showing weld metal, partial melting in the transition zone with second phase along the grain boundaries, and two holes in the plate material* ×80

dissolve in the aluminium crystals without reaching equilibrium.

As the cooling rate after welding is high, it is to be expected that the strength of this zone will be a little higher than that of the annealed material because there is slight ageing after welding.

The third zone, where the structure shows crystal growth, has to be divided into two parts. In the first part, a little more of the second phase is dissolved in the aluminium crystals. The second part of this zone will be heated above the eutectic temperature, with the result that a small amount of melt is found in the neighbourhood of the second phase.

In the macrograph of this zone, shown in Fig. 1, a small amount of melt is to be seen along the crystal boundaries. The amount of melt increases in the direction of the fusion line.

Heat-treatable alloys

There are two possible ways of welding heat-treatable alloys. If it is possible to apply an ageing treatment after welding, the best choice is to weld in the quenched condition (T4). If the workpiece is such that this post-heat-treatment is impossible, the welding must be done after the complete heat treatment (T6).

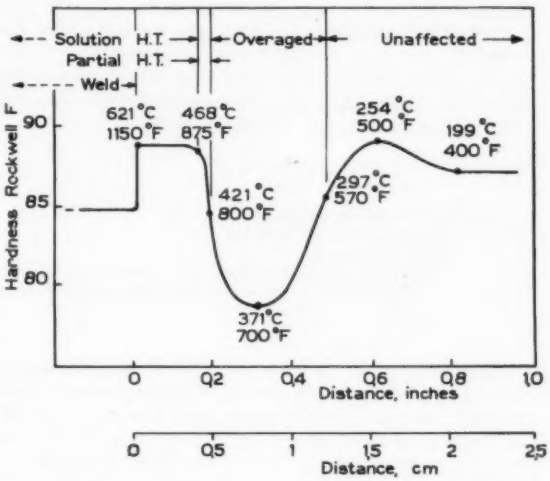
(*a*) *Welding in the quenched condition*

Welding in the quenched condition (T4) of the alloy 6061 has been investigated by Burch.[1]

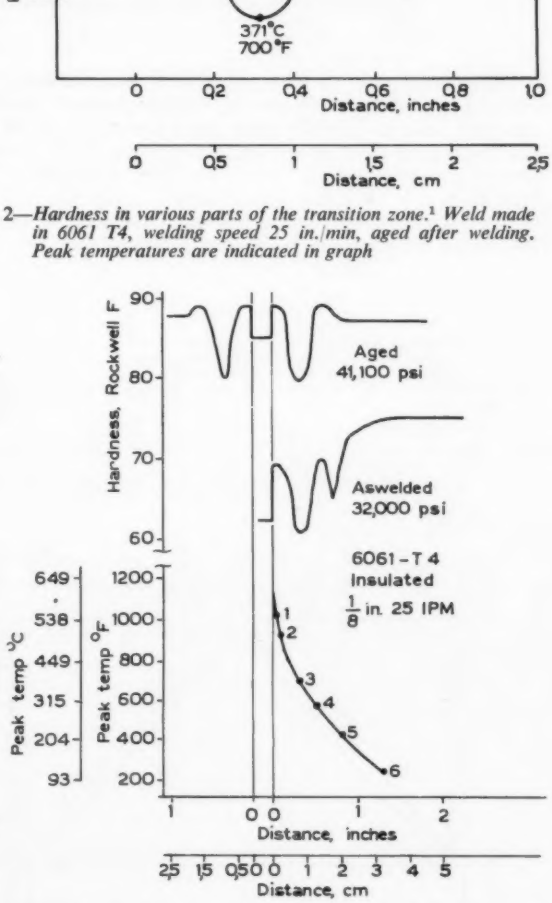
Starting again from the unaffected material, one finds a first zone in which the alloy is aged by the welding heat. In the adjacent zone, where the temperature has been higher, the alloy has been overaged to a certain extent, resulting in a lower hardness. At still higher temperatures there will be a zone in which the solubility of Mg_2Si is increased. After a rapid cooling the material in this zone will contain Mg_2Si as a second phase plus a super-saturated aluminium crystal. This zone will respond to an ageing treatment.

In the next zone the second phase will be completely in solution, the structure after welding being the T4 condition. If the workpiece is aged after welding, an increase of hardness will be found in all the zones. In the zone where, after welding, the quenched structure was present, the hardness will be equivalent to the completely aged material T6. In the zone where over-ageing was present, an increase in hardness will be found because the Al crystals are still super-saturated, but the hardness of the T6 condition will not be reached.

In theory a zone with an annealed structure should exist between the T4 structure and the over-aged material. Here, all of the second phase should be present as Mg_2Si crystals, and the Al crystals should be in equilibrium, with the result that the ageing effect would be zero. This zone is not found in the experiments of Burch (*see* Figs. 2 and 3). Obviously, the



2—*Hardness in various parts of the transition zone.*[1] *Weld made in 6061 T4, welding speed 25 in./min, aged after welding. Peak temperatures are indicated in graph*



3—*Hardness survey of 6061 weld before and after ageing, and peak temperature in relation to distance from weld*[1]

time at the annealing temperature has been too short to cause complete annealing.

Burch's experiments show that if the right filler material is chosen and combined with a certain dilution of the weld by molten plate material, the weld will respond to heat-treatment in about the same manner as the plate material. Figures 2 and 3 show that after ageing the weld material has the same hardness as the unaffected plate.

(*b*) *Welding after complete heat-treatment*

After welding a plate in the aged condition T6, the zone nearest to the weld will be in the quenched condition T4.

The next zone will be over-aged to an extent dependent on the time–temperature programme.

The work of Smith, Funk, and Udin[2] shows the dependency of the amount of over-ageing on time and temperature according to the formula

$$p = \int \exp -\frac{Q}{RT} \cdot dt$$

where p = an ageing parameter
Q = energy in cal per mole
R = gas constant
T = temperature (for the heating cycle during the welding process the temperature is a known function of time)
t = time

After welding it would be possible to give the workpiece a careful ageing treatment. This treatment should be such that the already aged plate material will not be over-aged. The result will be a welded joint in which the only weaker zone is the over-aged part of the joint.

Apart from these two zones on both sides of the weld, the material will be in the T6 condition.

To sum up, it can be stated that (apart from the technically unimportant annealed alloy) every weld in aluminium will contain a zone in which the strength after welding and ageing is decreased.

For different alloys the weak zone is caused by essentially different metallurgical processes, but the common cause of all changes is the thermal cycle during welding.

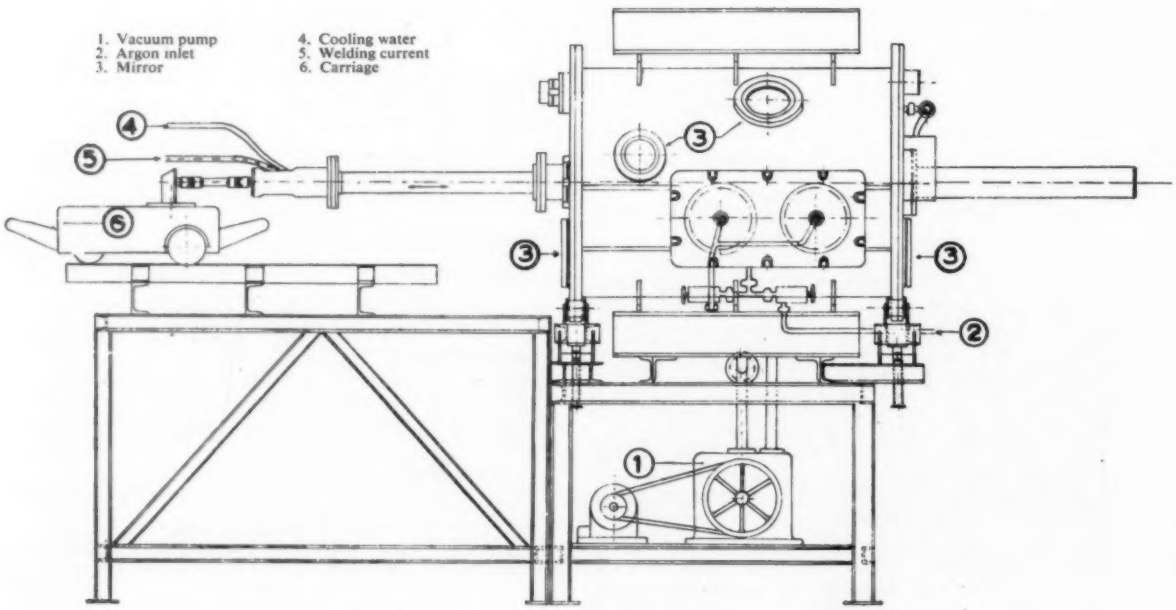
A further study of the influence of the welding heat on metallurgical changes will be important. The effect of heating for a very short time at a given temperature cannot be compared with the effects obtained in the normal heat-treating practice of aluminium alloys, since the time factor has a different order of magnitude.

Ageing time in heat-treating practice is, for instance, 8 hr at 170°C., whereas the time at the same temperature during welding is measured in seconds. It is therefore to be expected that phenomena such as ageing and over-ageing occur at temperatures which are higher than those expected from heat-treating practice.

Temperature limits

(*a*) *Width of heat-affected zone*

For the alloys used in welded structures, the temperatures at which structural changes occur are known. These temperatures are determined by laboratory tests where the material has been submitted to a certain temperature programme. As a rule, the heating and cooling rate and the time during which the material is at a certain temperature in these laboratory tests are very different from the values for a welding cycle. Consequently, the temperature limits usually mentioned for metallurgical changes cannot be applied to the welding process. Even at higher temperature it will be very unlikely that an equilibrium



4—*Apparatus for welding in argon atmosphere*

can be obtained, especially if certain components have to diffuse to a relatively long distance.

(*b*) *Relationship to welding conditions*

The width of a zone heated above a certain peak temperature can be calculated according to the formula of Rykalin[3]:

(i) For thin plate

$$b = \frac{q/v}{\sqrt{\left(\frac{\pi e}{2} C\gamma\delta\right)}} \cdot \frac{1}{T_m - T_o}$$

(ii) For thick plate

$$b = 2\sqrt{\left(\frac{q/v}{\frac{\pi}{2}.e.C\gamma} \cdot \frac{1}{T_m - T_o}\right)}$$

in which q = effective heat input in cal/sec
v = welding speed in cm/sec
T_m = peak temperature in °C.
T_o = temperature of plate at beginning of welding in °C.
b = width of the zone heated above T_m
C = specific heat of material in cal/g
γ = specific weight of material in g/cm³
δ = thickness of the plate in cm.

For specific material, a certain starting temperature (*e.g.*, room temperature) and specific peak temperature (*e.g.*, the temperature at which recrystallisation is obtained during the welding cycle), the formula can be simplified to:

(i) $b = \text{constant} \cdot \frac{q}{v\delta}$

(ii) $b = \text{constant} \sqrt{\frac{q}{v}}$

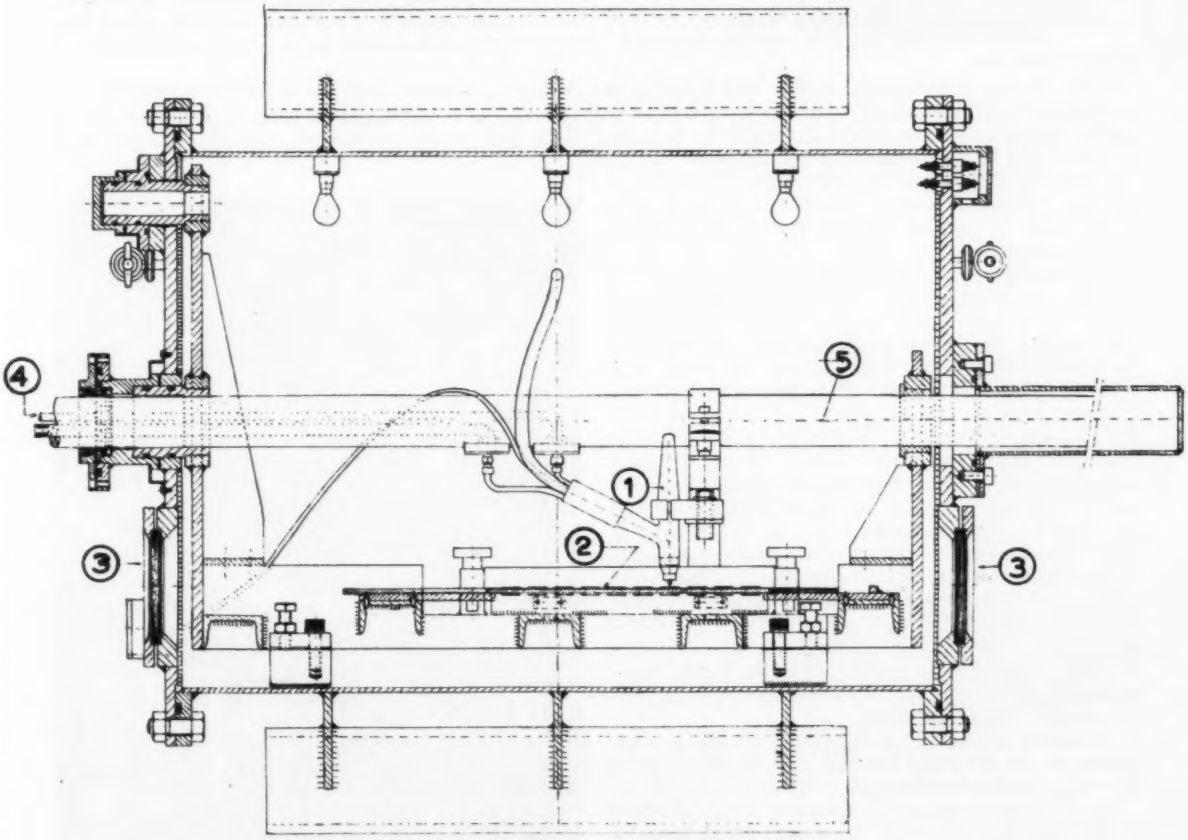
The width of the heat-affected zone is therefore determined by the quotient, q/v, *i.e.*, the amount of heat input per unit length of a weld.

To obtain a small heat-affected zone, the welding speed should be increased to its maximum obtainable limit at a certain energy level.

In certain cases it can be helpful to clamp the plates to be welded in a special jig, in which case the jig can add to the speed of cooling.

(*c*) *Influence of width of weakened zone on strength of welded joint*

Without any calculations it will be clear that if the welded zone is broad enough the joint will have the



5—Drum for making welding tests in pure argon

1. Welding torch
2. Test plate
3. Mirrors
4. Inlet of watercooling and welding current
5. Beam moving in horizontal direction for support of welding torch

strength of the weakest material. Broad in this connection means several times the plate thickness.

It is also clear that if the width of this zone is decreased to zero, the influence of the weak material will disappear.

Between these two extremes is an area where the geometrical form of the weakened zone decreases the possibility of yielding to a certain extent.

As a first approximation it can be stated that yielding cannot occur in the normal way if the width of the weakened zone is less than the plate thickness, since shear in a plane at an angle of 45° cannot occur without hindrance.

If the weaker zone is situated next to a weld with reinforcement, the tension will not be linear as a result of stress concentration. This will also cause an increase in the yield strength.

The general opinion that welded joints will always have the strength of the annealed material gives a safe guarantee, but in many cases it will be too pessimistic. Many recent publications, *e.g.*, Burch[1], prove that if the welding is done on a sound metallurgical basis much better results can be obtained.

Porosity in Aluminium Welds

Sources of hydrogen

There is almost general agreement that porosity in aluminium alloys is caused by hydrogen alone. The partial pressures of other gases above the Al melt are so small that they can safely be disregarded.

Carbon monoxide can be formed only in very small amounts because it is impossible to reduce aluminium oxide with carbon at the temperatures occurring during welding.

The nitrogen pressure will be small, because the nitride is stable. Hydrogen can dissolve into the weld metal from many sources. Firstly, the atmosphere surrounding the arc contains hydrogen or hydrogen compounds which are readily dissociated into atomic hydrogen. Hydrogen compounds (mainly water) can originate from the argon cylinder and more probably from moisture present in the different parts of the tubes and apparatus which conduct the gas from the cylinder to the orifice of the torch. Most aluminium welders realise the extreme importance of keeping these parts as dry as possible.

At the orifice of the torch the stream of argon comes into contact with the air. Owing to the rapid expansion of the gas in the arc and the nearly right angle between the gas stream and the work piece, the gas current will be strongly turbulent, with the result that a certain amount of air can be mixed with the argon. Consequently, water vapour can come into contact with the molten metal in this way.

A further amount of hydrogen is present in contaminants on the plate material and the filler wire if this is not cleaned sufficiently.

Even if the cleaning is correctly done, there is always the oxide layer on the metal, which contains water in the absorbed form and chemically combined water, *e.g.*, in Böhmit. Many aluminium alloys contain reactive alloying elements such as magnesium, or impurities such as sodium. These components will be present in the oxide layer in the form of hydroxide, where they contribute to the potential hydrogen content of the surface layer.

The last source of hydrogen is the gas dissolved in the atomic state in the metal.

During welding, the oxide layer will be broken and part of it will form a suspension in the liquid Al. All hydrogen components present will react with liquid aluminium during this process, with the result that hydrogen is dissolved in the melt.

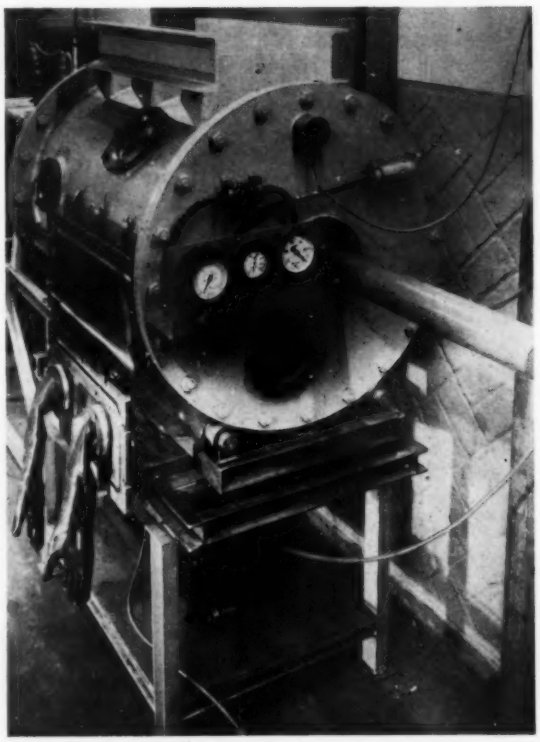
Table I

Hydrogen in aluminium

T, °C.	*Solubility, ml per 100 g metal*	*Diffusion coefficient, cm²/sec*
0	<0·001	$1·9\ 10^{-22}$
100	<0·001	$2·9\ 10^{-15}$
200	<0·001	$4·0\ 10^{-11}$
300	0·0015	$2·0\ 10^{-8}$
400	0·005	$1·6\ 10^{-6}$
500	0·013	$4·1\ 10^{-5}$
600	0·025	$4·9\ 10^{-4}$
660 solid	0·035	$1·7\ 10^{-3}$
660 liquid	0·75	—
700	0·9	—
800	1·7	—
900	2·8	—

Solubility and diffusion coefficient of hydrogen in aluminium

In Table I the values of solubility of hydrogen in solid and liquid aluminium and the coefficient of



6—General view of apparatus for welding tests in pure dry argon

diffusion of hydrogen in solid aluminium at different temperatures are given. If these values are compared with those for hydrogen in steel, where the influence is much more familiar to welding experts, it is found that solubility of hydrogen in aluminium is much smaller than it is in iron and that the diffusion, especially at lower temperatures, is extremely difficult. Only at temperatures above 500°C. can hydrogen in Al diffuse as easily as hydrogen in steel.

Experimental work on porosity

The further investigation of the different factors which contribute to the final hydrogen content of the weld and therefore to porosity seems important enough. At the moment it is still very difficult to obtain reliable results where so many factors are involved, and the methods for the quantitative measurements are very scarce.

To start at all, it is necessary to exclude many of the contributing sources of hydrogen, thereby simplifying the problem to the influence of hydrogen in the metal and hydrogen in the oxide layer. For these purposes a welding torch is placed in a closed drum filled with carefully dried hydrogen. The torch can be moved over the test plates by mechanical means, thereby making a weld under controlled conditions. The apparatus made for this purpose is shown in Figs. 4–6.

Conclusions

As the programme of welding tests is far from complete, only a few preliminary results can be given at the time of writing this report.

(*a*) If the argon is as dry as possible, the weld will show no porosity visible in a radiograph even if the material is fused several times
(*b*) Even a small amount of moisture in the argon will cause severe porosity in the weld
(*c*) In the heat-affected zone, porosity in the form of small holes of about 0·1 mm dia. is found in the metal which is not melted during welding
(*d*) These holes can also be found if the metal is heated to a temperature not far below the melting point according to a time–temperature programme such as that used during welding.

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LETTER TO THE EDITOR

Quality Control

The modern approach to the control of welding standards, as carried out by the leading inspection companies of this country, such as ourselves, is of course for all welders to be tested, in each of the positions required, before he is allowed to take up his duties.

This action sets the standard before any welding takes place, radiography and/or ultrasonics being ancillary tasks, the main function of a welding inspector/radiographer being to supervise the joint set up, line up, tacking; and, if required, the application of the root run, or stringer, in stovepipe welding. His duty, in the interests of the job, is to check all welding as it is done, from start to finish, using radiography and/or ultrasonics merely as methods of checking that the required standard is maintained, and to ensure that a lasting record is kept, in the form of radiographs.

The welding inspector therefore *must* be a capable, if not a first-class welder, fully conversant with all the important aspects of welding, both in theory and in practice, so that the best possible standard is achieved.

This important fact is still not appreciated by some firms, who still employ either unskilled local labour from labour exchanges, or very much junior types with no background, where these men are turned loose as inspectors, under the supervision of a radiographer, also without welding experience or knowledge. This practice of unskilled and unqualified labour being turned loose on welding, whether it be important pipelines for fuels or gas, tanks or vessels, applies to both the radiographic and the ultrasonic fields, and it does seem farcical that such labour is tolerated.

A little knowledge is dangerous, and never was this more true than in the welding field, or in the inspection of welding field.

In the hands of a craftsman, welding is one of the finest tools available to man, but in the hands of a dilutee, trainee, etc. it becomes a tool of sabotage, if only of a company's products and good name. This also applies to the position of qualified and unqualified inspectors, and it seems tragic that we must have a major disaster occurring through lack of qualified welders *and* inspectors, before people will realise the great need for *qualified inspection*.

Honesty of purpose is the first essential in inspection, and this of course starts by having qualified inspectors, with a wide knowledge of the subject to be inspected.

Then, and only then, can non-destructive examination be used on a reduced percentage, as has been suggested for pipeline welds, when a visual inspection of the internal penetration bead has been made through the unwelded portion.

It comes to a poor state of affairs when so called "welding supervisors/radiographers" have to ask a stovepipe welder to interpret the radiographs taken of his own welding—but this does happen!

R. B. WHALLEY
(*Inspection Services Limited*)

Welding RESEARCH

B.W.R.A. REPORT

Temperatures in Spot Welding

By
J. A. Greenwood, PH.D.

Previous work on the temperature in spot welding has been based on simple models of the heat distribution which lead to analytical formulae for the temperature. In this paper a numerical solution of the problem is described. This was established with the aid of a digital computer. The flexibility of numerical methods allows the actual geometry involved to be taken into account; this means that the distribution of the resistive heat generation and the cooling effect of the electrodes are included. Contact resistance and the variation of material properties with temperature could have been included, but were ignored in this first study.

It was found that the temperature distribution during the heating corresponds initially to the distribution of heat production, with spikes of high temperature at the current concentrations. These spikes are soon levelled out by conduction and, as heating continues and conduction can take place over larger distances, the temperature tends to an equilibrium form with the greatest temperatures on the axis of symmetry. This form bears no resemblance to the heat production pattern.

Cooling curves have been plotted for various weld conditions, and the cooling rates found at different points. Cooling is fast by comparison with normal metallurgical processes, with rates of the order of 1000°C. per second. The cooling rate does not change much as the weld time is varied within the usual range, but it is very dependent on the time between switching off the current and unclamping the electrodes.

TO OBTAIN spot welds of satisfactory quality, particularly in the newer engineering materials, it is essential that a close control should be kept over the temperature variation. One difficulty, however, is that the effects on the temperature distribution of changes in the welding time and current are not completely understood. The only published theories of the process (*e.g.*, Roberts, Roberts and Wells[3]; Adasinskii[1]) are based on highly simplified models which ignore not merely the detailed mechanism of the heat generation, but also the actual geometry involved. These models are certainly a useful guide to what may be expected, but they are too far from practice to be intrinsically convincing, and there is only a limited amount of experimental work on temperature measurements available with which to compare them.

It is possible by numerical methods to obtain a solution which includes all the known facts and makes assumptions only where the facts are not known. Using a digital computer this involves little more work than is needed to evaluate the analytical formulae of the simple models. However, since there is a serious lack of information about the physical process of welding, particularly on the breakdown of contact resistance, it was decided to produce a pilot solution before attempting to make postulates describing the general process. In this pilot solution the true spot welding geometry is taken into account, but not the

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properties of the material. This reduces the problem to a linear one in which the principle of superposition applies. If a solution for a particular current has been worked out then the solution for any other current is found by simply scaling all the temperatures by a constant factor. In addition, by superposing a 'negative heat source' the cooling curves can be found from the heating curve. If $\theta^H(t)$ is the temperature at time t due to switching on the current when $t=0$ and leaving it on, then the temperature at t after the current has been switched off again at $t=t_0$ is

$$\theta(t) = \theta^H(t) - \theta^H(t-t_0)$$

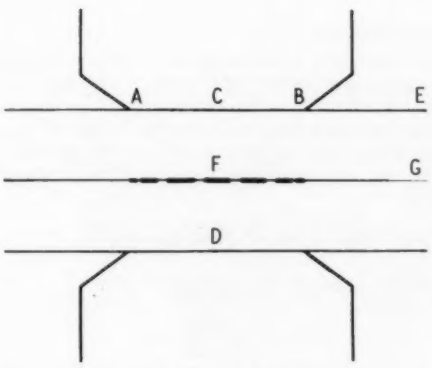
Thus, from a single heating curve, all the heating and cooling curves for arbitrary currents and heating times can be found. The solution becomes generally applicable instead of referring only to one particular material, current, and weld time as would be the case if, for example, the variation of specific heat with temperature had been introduced. Unlike the analytical solution, the numerical method does not require assumptions of constant material properties and there would be no difficulty in extending it to produce solutions for specific problems as required.

The Physical Problem

We are concerned with the problem of spot welding two large sheets of equal thickness, using circular electrodes placed opposite each other (Fig. 1). It is generally accepted that the sheets make electrical contact over a circle of approximately the same diameter as that of the electrodes.

Heat production

The current spreads out as it passes through the sheets, and in the absence of contact resistances there would be current concentrations round the periphery of the three contacts. On the other hand, in the absence of 'body' resistance, the potential drop would be uniform across each contact circle, so that the current density there would vary inversely with local contact resistance. The whole problem of whether oxide breaks down uniformly, or otherwise, for example preferentially round the edge of the contact circle, is being studied, but it is clearly too complex to be included in a pilot numerical solution, so it will be assumed that the contact resistance is zero. (This is, of course, exactly true in practice for the post-heat-treatment of a weld, when it is clearly desirable to know the cooling rates.) The distribution of current can then be found, again by numerical methods, and from this the rate of heat production at each point is known.



1—*General diagram of spot weld*

Heat loss

Heat is lost by conduction into the distant parts of the sheets and into the electrodes; in addition, the sheets may lose heat from the surface by convection and radiation. By considering a specific case, however, this heat loss from the surface can be shown to be negligible.

Around a $\frac{3}{8}$ in. dia. electrode there is a red-hot ring less than $\frac{1}{8}$ in. wide, so that its area is less than 1 sq.cm. If its temperature is 800°C., the radiation loss can be calculated from Stefan's law to be at most 1·8 cal/sq.cm sec, or 7·5 W/sq.cm. If the remainder of the surface reaches 200°C., the radiation loss will be 0·3 W/sq.cm. Convection loss will be of the same order, so that from 20 sq.cm the loss will be 12 W, making in all 20 W. Since the aim is to heat a nugget with a volume of approximately 0·3 cu.cm to over 1500°C., the quantity of heat needed is of the order of 2500 joules, so that the loss from the surface, even over a second (20 joules) is negligible.

It is immediately clear that the heat loss into the electrode must be important, for otherwise the electrode contact area would reach the same temperature as the weld interface. The thermal resistance, like the electrical resistance, has a contact term and a body term. Values for the thermal conductance between two metals quoted by Weills and Ryder[4] (not for the pressures used in welding) range from 0·05 to 1·5 cal/sq.cm °C. sec; under welding conditions a value of 2 seems likely. The thermal conductance of the electrode itself is of the same order; for example, a copper electrode which is water-cooled 1 cm from its tip has a conductance of 1 cal/sq.cm °C. sec, so, combining these, the conductance into the electrode is 0·67 cal/sq.cm °C. sec. The additional heat used to heat up the electrode is neglected, since it is important only in initial stages of heating, and during this period a larger error has already been introduced by neglecting the heat produced in the contact resistance. If the electrode is a much better conductor than the weld sheet, heat will flow in the electrode parallel to the interface and will tend to equalise the temperatures over the contact area. This will be ignored, and it will be simply assumed that the normal component of the heat flow into the electrode at any point of the contact area is directly proportional to the temperature at that point. The previous figure of 0·67 cal/sq.cm °C. is taken as the constant of proportionality. It turns out that the temperatures over the electrode soon become nearly uniform anyway, so that the cross-flow cannot be important.

The main loss of heat from the weld nugget is straightforward conduction to more distant parts of the sheets. This is governed by the ordinary heat conduction equation with an additional term giving the rate at which heat is produced at each point; the

numerical solution of this equation is the basis of this work.

The assumptions introduced are:

(i) No contact resistance. This means that the current distribution can be calculated from the geometry of the weld and that there is no localised heat production at the contact surfaces
(ii) Constant material properties, *i.e.*, thermal conductivity, specific heat, and electrical resistivity. This allows scaling and superposition to be used
(iii) No heat loss from the free surface: conduction into the electrode at a rate proportional to the temperature at the electrode contact.

Numerical Procedure

Cylindrical co-ordinates (r, z) are used, where z is the distance along the axis of symmetry and r the distance from it. Then the temperature satisfies the equation:

$$\frac{c\partial\theta}{\partial t} = \lambda\left(\frac{\partial^2\theta}{\partial r^2}+\frac{1}{r}\frac{\partial\theta}{\partial r}+\frac{\partial^2\theta}{\partial z^2}\right)+A(r,z) \quad \ldots\ldots\ldots \quad (1)$$

where A is the rate of heat production, c the specific heat for unit volume, and λ the thermal conductivity. Over the electrode surface AB (Fig. 1) $\lambda\dfrac{\partial\theta}{\partial z}=H\theta$ where H is the thermal conductance into the electrode, and over the free surface BE and also over the interface FG (by symmetry), $\partial\theta/\partial z=0$. There is axial symmetry about the line CD joining the centres of the electrodes, so if the temperature is known over any plane through this axis, it is known everywhere.

To solve these equations numerically a mesh of points covering one such plane is considered. If an arbitrary point is labelled 0 and its four nearest neighbours as 1, 2, 3, 4 (Fig. 2), then by using Taylor's theorem to express the temperatures θ_1 to θ_4 in terms of θ_0 and its derivations, it can be shown that, if h is the mesh size,

$$\left.\frac{\partial^2\theta}{\partial r^2}+\frac{1}{r}\frac{\partial\theta}{\partial r}+\frac{\partial^2\theta}{\partial z^2}\right|_0=\frac{1}{h^2}\{\theta_1+\theta_2+\theta_3+\theta_4-4\theta_0+\frac{1}{2n}(\theta_4-\theta_2)\}+\frac{h^2}{6n}\cdot\frac{\partial^3\theta}{\partial r^3}+0(h^4)$$

where nh is the distance of point 0 from the axis.

Also,

$$\theta_0(t+\Delta t)-\theta_0(t)=\Delta t\cdot\frac{\partial\theta}{\partial t}+0(\Delta t)$$

so that equation (1) becomes

$$\theta_0(t+\Delta t)=\theta_0+\frac{\lambda\Delta t}{ch^2}\{\ldots\}+0(\Delta t^2)+\frac{\lambda h\Delta t}{6cn}\cdot\frac{\partial^3\theta}{\partial r^3}+0(h^4)+\frac{A\Delta t}{c}$$

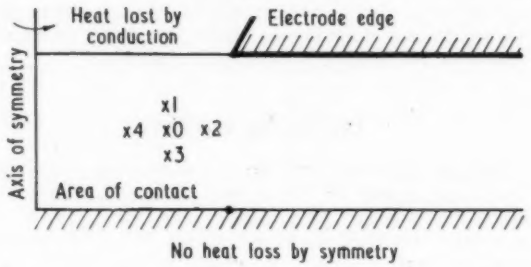
or approximately

$$\theta_0(t+\Delta t)=\theta_0+\gamma\{\theta_1+\theta_2+\theta_3+\theta_4-4\theta_0+\frac{1}{2n}\cdot(\theta_4-\theta_2)\}+A\Delta t/c \quad \ldots\ldots\ldots \quad (2)$$

where γ has been written for $\lambda\Delta t/ch^2$.

This equation predicts the temperature at time $(t+\Delta t)$ from the temperatures at time t, and so by repeated application the temperature of each point at any time can be found. Suitable values of h and Δt must be chosen; these must be small so that the terms neglected are in fact negligible, but, of course, the smaller they are, the more points and time intervals will be needed, so that more work has to be done. The values of h and Δt cannot be chosen independently, as the solution becomes unstable if γ is too large. It can be seen that values of γ greater than $\frac{1}{4}$ are rather suspect at the coefficient of θ_0, namely $(1-4\gamma)$, would then become negative, in which case the equation states that the hotter a point is at time t, the colder it will be at time $(t+\Delta t)$ (Dusinberre[2]). The most accurate solution has γ considerably smaller than this, though the optimum value is not known. Thus, if the number of points needed to represent the true solution is chosen so that the size of the space interval is known, then to reduce the length of the calculation the time interval must be as large as possible. But as the time interval is increased, the solution becomes first inaccurate and then unstable. As a compromise between an excessive amount of calculation and reasonable accuracy, the time interval was chosen to make γ equal to 0·2 in all cases.

Usually the temperature variation across the sheet may be represented adequately by the temperature at four points. For mild steel $\frac{1}{8}$ in. thick this means that



2—*Section through sheet, showing typical mesh point and its nearest neighbours*

the time interval is 0·0125 sec. For the initial heating this is not detailed enough, and some calculations were done with eight points across the sheet; the two distributions agreed after moderate time values. The ratio of electrode diameter to sheet thickness, 11:4, was that appropriate to welding $\frac{1}{8}$ in. thick sheet. Temperatures were calculated at points up to ten sheet thicknesses from the axis of symmetry; at this radius an additional boundary condition of no heat loss was imposed. The results justified this step as the temperature attained at the boundary was negligible during the time range of interest.

Boundary conditions

The boundary conditions at the electrode and the free surface are introduced in the following way. The mesh is fitted so that the surface of the weld sheet lies halfway between two mesh points. Then if θ_A is the temperature of a typical point just inside the sheet, and θ_B the temperature at a 'fictitious' point just outside:

$$\left.\frac{\partial\theta}{\partial z}\right|_s\approx\frac{\theta_A-\theta_B}{h}, \text{ and } \theta_s\approx\frac{\theta_A+\theta_B}{2}$$

But $\lambda\left.\dfrac{\partial\theta}{\partial z}\right|_s=H\theta_s$, so eliminating θ:

$$\theta_B=\frac{1-hH/2\lambda}{1+hH/2\lambda}\cdot\theta_A.$$

This is applied over the electrode with H=0·67, and

over the free surface with $H=0$. This equation is the only place in the mathematical problem where the sheet thickness or thermal conductivity is introduced explicitly. If d is the sheet thickness and $h=d/N$, then the results of one calculation will apply to any weld with the same value of $dH/N\lambda$. If it were not for the practice of changing the ratio of electrode diameter to sheet thickness when the sheet thickness changes, the results for $\frac{1}{8}$ in. mild steel ($\lambda=0{\cdot}12$) could be applied to $\frac{1}{24}$ in. stainless steel ($\lambda=0{\cdot}04$). Some scaling would, of course, be necessary in both time and temperature: the time scale is proportional to ch^2/λ and, for a given current, the temperature is proportional to $\rho/h^2\lambda$.

Outline of calculation

The first step is to evaluate the rate of heat production (ρJ^2) at each point. The potential distribution in spot welding geometries was known from earlier work with the T.I. computer: the values of the current density J were found from this by numerical differentiation and a table of values of $(J/J_m)^2$ produced. J_m is the mean value of the current density, *i.e.*, the total current divided by the electrode area. The heating term $(A\Delta t/c)$ in equation (2) can be written as

$$\gamma(J/J_m)^2.\rho J_m^2h^2/\lambda.$$

The factor $\rho J_m^2h^2/\lambda$ is ignored in the calculation, but it can be reintroduced at the end to convert the numbers found into temperatures.

The computation proper was made on the Ferranti 'Mercury' computer installed at Oxford University. The computer reads a punched tape containing the parameters specifying the geometry, the heat loss coefficient at the electrode, the table of heat production rates, the mesh points at which the temperature is to be printed, and the times at which these temperatures are required. The programme has provision both for heat loss into the electrode and the simpler case of no loss; one may be changed to the other at a specified time to represent unclamping the electrodes. Similarly, the heat production can be set to zero at any specified time, so that heating and cooling curves can be obtained directly as well as by using the superposition principle already explained.

Results

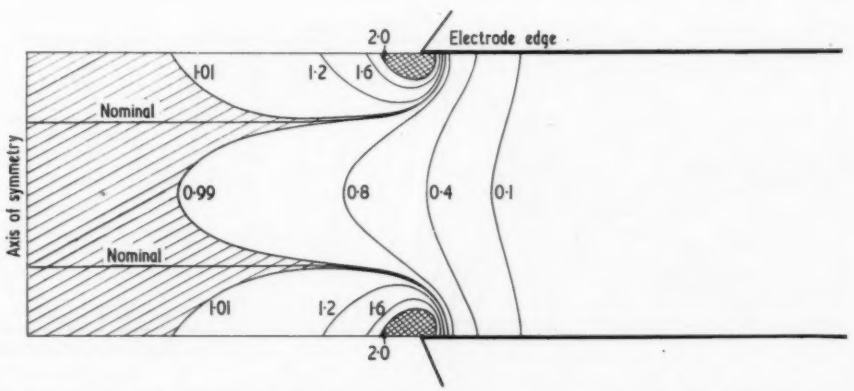
The computer prints the temperatures of selected points at selected times. Two types of graph can be plotted from this information; the first is the temperature distribution after various times, and the second is the temperature cycle of a particular point. The latter is of more interest to the metallurgist, but it is rather easier to see what is happening physically with the first type. Before giving either it is convenient to describe the pattern of heat production that occurs.

Distribution of heat production

The current does not flow straight across the weld sheet but spreads out to make use of the regions not directly under the electrodes. Most of the current that does spread enters the weld sheet near the edge of the electrode, causing a current concentration there. Near the centre of the electrode the spreading has little effect particularly if the ratio of electrode diameter to sheet thickness is large, and the current is uniform. This gives rise to the heat production pattern of Fig. 3. Over almost all the region under the electrodes the rate of heat generation is within $\pm 20\%$ of the nominal value, *i.e.*, the value due to the same potential difference applied to a cylinder of the same height. Over a considerable volume it is within $\pm 1\%$. Only immediately under the edge of the electrode and at the edge of the contact circle is the rate appreciably higher; and these regions are close to areas with very little heat production.

Temperature distribution at the end of heating

Figure 4 shows the temperature distributions existing at different times during the heating. The temperatures have been so scaled that at each time the hottest point is at 1600°C., and the diagrams thus give the temperature distribution as the current is switched off for a range of weld times. Figure 4*a* shows the temperatures produced after a very short weld time (0·025 sec). The resemblance to Fig. 3 is quite marked in the lower half although, even after such a short time, conduction has shifted the hottest point from the edge of the contact circle towards the centre. There is a second temperature maximum in the upper half of the

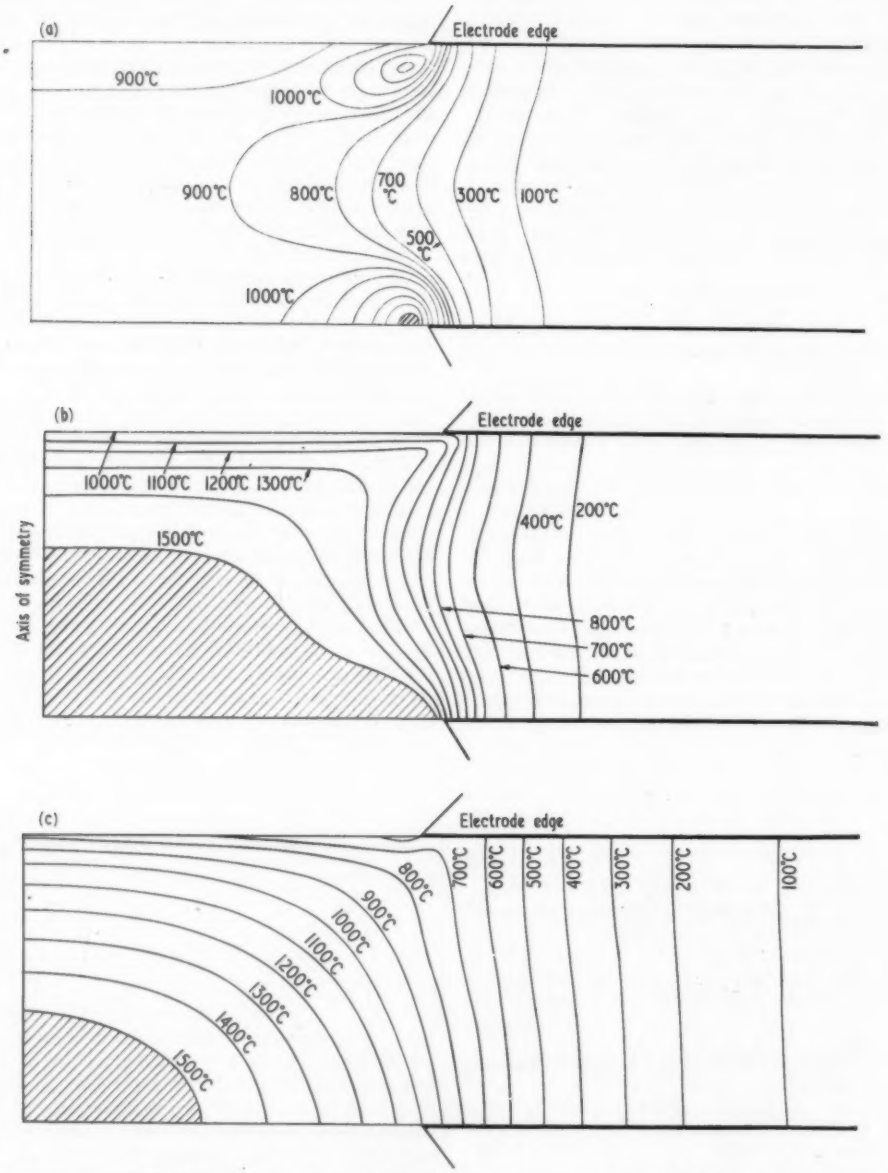


3—*Distribution of heat production. Numbers at contours give fraction of nominal heat production rate*

diagram, but the heat lost to the electrode has reduced the maximum value, and shifted it away from the electrode interface into the body of the sheet. Along the axis of symmetry the temperature varies little; it is very nearly constant until near the electrode, where it decreases rather abruptly by 10%.

Figure 4*b* shows the effect of a longer weld time; it corresponds to the shortest time normally used on an a.c. welder for this thickness (0·1 sec). There is a very large region near the maximum temperature, extending for half the thickness of the sheet in the middle and out as far as the edge of the contact circle. The actual maximum occurs on the interface at approximately two-thirds of the electrode radius.

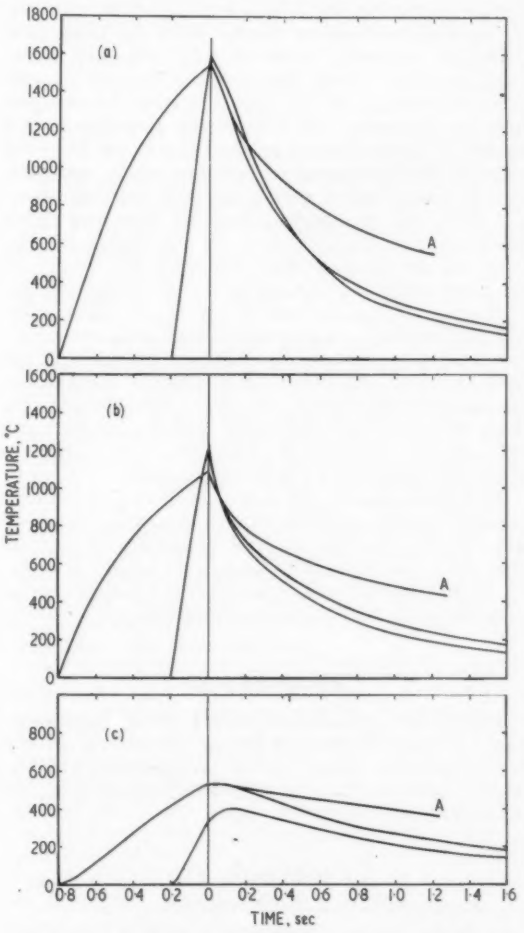
As already discussed, it is not necessary for any particular isotherm to be associated with any particular temperature. A larger current could bring the isotherm marked 900°C. to 1500°C., giving a very large molten region. The isotherm marked 1500°C. would then reach 2500°C., which might be undesirable; the coefficient of expansion in the liquid state is likely to be much larger than for the solid, so that large strains would be expected. However, the expansion due to heating above the melting point is likely to be



4—*Temperature distribution after various weld times:* (a) *0·025 sec;* (b) *0·1 sec;* (c) *0·6 sec. In each case the current is chosen to bring the hottest point to 1600°C.*

less important than the expansion occurring on melting, which will occur whatever the temperature distribution at the end of heating. A more important objection to the distribution in Fig. 4*b* is that the hottest region is still close to the edge of the contact circle. If melting and consequent expansion occur, there is likely to be expulsion of the molten material because of the absence of a retaining ring of solid material.

Figure 4*c* corresponds to a normal weld time for $\frac{1}{8}$ in. sheet (0·6 sec). The hottest isotherms, as for the usual weld nugget, are roughly oval. Near the electrode the isotherms are straight and parallel to the electrode face, showing that the dominating feature of the heat flow is conduction to the electrode. A short distance outside the electrodes the isotherms begin to run straight across the sheet thickness and the temperature falls rapidly; at a distance of twice the electrode radius the temperature is less than 100°C.



5—*Temperature cycles at different points. The current is switched off at the zero of the time scale; the curves marked* A *show the effect of unclamping the electrodes at the same instant*

(*a*) Near centre of weld spot
(*b*) Near edge of weld spot
(*c*) Outside heat-affected zone

If melting occurs after a time of this order it will be near the axis, and will spread out to the edge of the contact circle only if the current is excessive. Under these conditions the molten metal is safely held in by a pressure seal, and expulsion is unlikely.

Thus it is seen that the temperature distribution continually changes during the heating, even though the heat production remains constant. Initially the hottest points are at the edges of the contact circle; they move inwards and finally the centre is the hottest. For each sheet thickness this process sets a definite minimum to the time in which a weld can be made.

Temperature cycles of individual points

The second way of presenting the results is used in Fig. 5. This shows the variation with time of the temperature at three typical points in the weld; one near the centre of the weld nugget, one near the edge of the weld spot, and one outside the weld spot.* The temperature cycle is shown for a long weld time (0·8 sec), and for a short one (0·2 sec), with the currents scaled to give the same maximum temperature, 1600°C. In addition, for the long weld time, the figure shows the cooling curve when the electrodes are unclamped as the current is switched off. (Zero clamping time would not be used in practice, but the curve for the shortest practicable time would be similar.)

It is seen that for points inside the weld spot the initial cooling rate is very high. For example, with the long weld time a point near the edge of the weld spot cools 100°C. in $\frac{1}{25}$ sec. Unclamping the electrodes has no effect for the first $\frac{1}{10}$ sec, but after this the cooling rate is appreciably reduced. Thus, for the first 0·1 sec, the cooling of points near the weld interface is due entirely to conduction within the sheets being welded. It is interesting to note that the initial cooling rate at the edge of the weld spot is almost twice as great with a short weld time as with a long one, while at the centre of the weld the cooling rate with the short weld time is slightly the lower. Although at first sight surprising, this is a natural consequence of such temperature distributions as those in Fig. 4. The temperature gradients shown there vary in just the way described for the initial cooling rates.

Figure 5*c* shows that the temperature of points outside the weld spot continues to rise after the current is switched off. The time at which the peak temperature occurs depends on the weld time and the distance from the axis. However, this effect is not appreciable except for points where the peak temperature is low; in general, it can be stated that if the peak temperature is more than half the value in the centre of the nugget it will occur at the end of the weld time. This may be a useful rule to bear in mind when using approximate models to calculate temperatures.

The most important cooling rate in welding steel is not the initial rate but the rate immediately below the Ae_1 temperature. Near the edge of the contact circle, the time to cool from 700° to 600°C. was 0·09 sec with the short weld time, and 0·11 sec with the long

* These curves give the temperatures due to a direct current. The curves produced by an alternating current of the same r.m.s. values oscillate about the d.c. curves with a frequency of 100 c/sec. For the sheet thickness considered ($\frac{1}{8}$in.), or for any greater thickness, the amplitude of the oscillations can be shown to be small.

weld time; with the electrode unclamped as the current was switched off the time increased to 0·26 sec. Near the centre of the contact circle the cooling rates were slightly higher; the times taken were 0·07, 0·09, and 0·25 sec respectively. All these cooling rates are fast compared to the cooling rates normally occurring in the heat treatment of steel (or in arc welding) and would seem likely to lead to transformation to martensite even in low-carbon 'non-hardenable' steels.

It can be seen from Fig. 5 that for cooling with the electrodes remaining on, points outside the heat-affected zone cool less rapidly than points inside it. At the greatest time given, the temperatures at the three points shown are approximately equal. After a longer cooling period, the point outside the heat-affected zone will be the hottest. This corresponds to the heat which has spread out into the sheet turning round and flowing back to the middle; in the end all the heat must flow out through the electrode. This is rigorously true in the problem solved and it is very nearly true in practice.

Conclusion

The temperature distribution in spot welding can conveniently be found by numerical methods, provided that a digital computer is available. The calculations apply to the actual weld geometry, and the results fit in with what is known experimentally. This is in contrast to the analytical solutions of simple models of the welding process, which by their nature can predict temperatures only in specified regions and, with most models, only during the cooling period.

The temperature patterns produced show that initially the maximum temperatures are located in a ring round the edge of the contact region. The bulk of the region between the electrode is at a uniform temperature, corresponding to the uniform rate of heat production there. If the current is high enough to produce a weld in such a short time it will be a ring weld.

With a rather longer weld time, the heat no longer remains where it is produced but has time to conduct over short distances. The original ring of high temperatures is close to a low-temperature region, so the ring disappears. There is also some conduction to the electrode, so that the upper half of the sheet becomes appreciably cooler than the lower half, but there is still a large region of uniform temperature.

At the weld times normally used, conduction takes place over large distances. The temperature pattern no longer resembles the heat production pattern; in fact it is hard to see any connection between them. The isothermals, as for the usual weld nugget, are ovals. The maximum temperature is at the centre of the weld, and both the electrode interface and the edge of the contact circle are very much cooler than this.

When the current is switched off, the cooling rates are extremely high. For $\frac{1}{8}$ in. thick mild steel the initial rate is over 2000°C./sec, and when the temperature has fallen by half, cooling rates of about 1000°C./sec are found. The cooling rate does not change much between different points in the weld nugget, or with different heating rates. A lower heating rate increases the time at high temperature, but mainly by taking longer to heat up, not by taking longer to cool down. The cooling rate is reduced if the electrodes are unclamped as soon as possible after the current ceases. This does not affect the initial cooling rate at points in the weld interface, but at the Ae_1 temperature the rate is reduced to 400°C./sec. This is still fast enough to give a martensitic structure in almost any steel.

The results described refer to a particular ratio of electrode diameter to sheet thickness and to a particular electrode loss coefficient, both appropriate to welding $\frac{1}{8}$ in. thick mild steel. It can be seen qualitatively how the picture would be altered for different materials without repeating the calculations. Changes in resistivity merely affect the size of the temperature produced by a given current, not the pattern of the temperature distribution.

The effect of a change in thermal conductivity is complicated. If the ratio of the thermal conductivity to the product of sheet thickness and thermal conductance into the electrode is kept constant, a change in thermal conductivity merely alters the time scale. Thus, for example, stainless steel will take about twice as long as mild steel to reach the quasi-equilibrium distribution. If the value of this ratio is changed, both the time scale and the pattern of isotherms will change. A lower thermal conductivity in the sheet will increase the importance of heat loss to the electrode, so that in equilibrium the temperature gradient across the sheet will be larger compared with the radial gradients. However, because of the change in time scale, the distribution at a given time will tend to be the more uniform one associated with an earlier stage in the heating process, and it is not clear which effect will predominate. An increase in sheet thickness with a corresponding increase in electrode diameter would have the same effect, but the practice of keeping not the diameter but the electrode *area* proportional to the sheet thickness will counteract this. The main result of increasing the sheet thickness is a simple increase in the time scale of the welding process.

These calculations are a first attempt at a detailed account of the temperature cycle in welding. They do not include detailed material properties or contact resistance. But even with these restrictions it is believed that they provide a useful step towards a fuller understanding of the spot-welding process.

Acknowledgment

The author is grateful to Dr. J. B. P. Williamson for many helpful discussions during the course of this work, and for advice on its presentation. He also thanks the Chairman of Tube Investments, Ltd., for permission to publish this paper.

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B.W.R.A. REPORT

Experiments on Fabricated Pipe Bends

By P. H. R. Lane, B.SC. (ENG.), A.I.M.,
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Gusseted pipe bends fabricated by welding from sections of 12 in. bore straight pipe have been subjected to in-plane bending moments and to internal pressure. Bends of the two-weld and three-weld designs have been studied, and the stress distributions determined are presented for the three types of loading.

The results achieved are compared with theoretical smooth-bend behaviour, and the nature of the divergence is discussed.

THE problem of the stresses and flexibility of smooth pipe bends of the welding elbow type, subjected to in-plane bending and to internal pressure, has been fully investigated under both static and dynamic loading. Static loading tests on smooth bends from 3 in. to 12 in. dia. are described in two papers by Gross and Ford.[1] In these tests, measurements of stresses on both surfaces of the bends were made, together with measurements of flexibility and changes in diameter. A mathematical analysis was developed which enabled the stress distribution in the pipes to be forecast fairly accurately.

Three series of fatigue tests on welding elbows made from 6 in. Schedule 40 pipe have also been carried out.[2] In the first of these, the bends were subjected to alternating in-plane bending, in the second they were subjected to pulsating internal pressure, and in the third to alternating in-plane bending combined with static internal pressure. As a result of these tests, a fairly complete picture of the behaviour of smooth pipe bends has been obtained.

Such smooth bends are normally only available in pipe sizes up to 24 in. dia. Above this size it is generally necessary to use some sort of fabricated bend. The present work is concerned with an analysis of the flexibility of such bends and the stresses existing in them. In general, fabricated bends can be divided into two groups. First, the group which has been described as 'gusseted' bends, and second, bends of the 'cut and shut' type. The gusseted bend is formed from lengths of straight pipe cut with their ends at suitable angles to their axes and welded together (Fig. 1). The 'cut and shut' type of bend is formed from one length of straight pipe by cutting out 'melon slice' pieces from what is intended to be the inner arc of the bend, then creasing the bend round and welding up where the pieces were removed. The present investigation is concerned only with the former type of bend.

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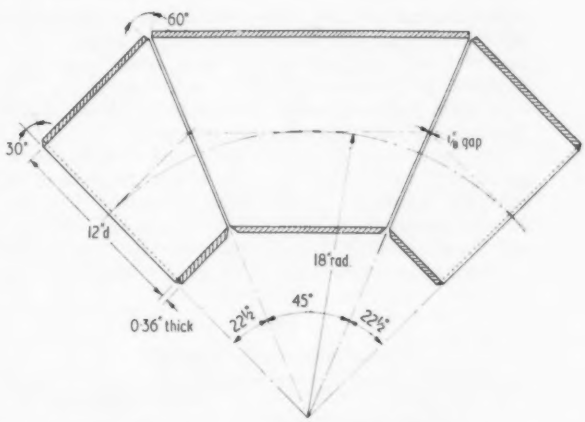
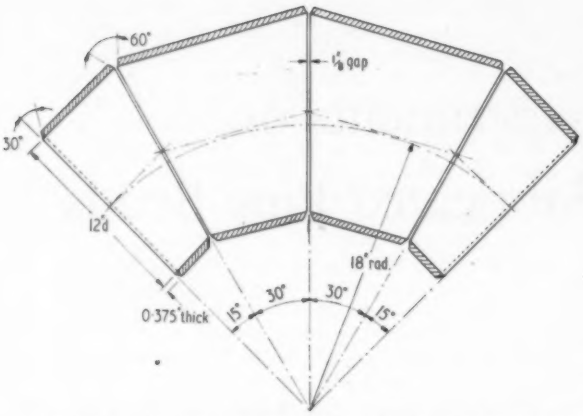
Previous Work

Very few stress analysis tests have been made on gusseted bends. One such test by Gross was included in the series of tests on smooth bends referred to earlier.[1] However, the main purpose of this test was to measure the flexibility of the bend, and stresses were measured only in the centre of a segment of straight



Welding procedure: Root run 12 s.w.g. back chipped; sealing run 12 s.w.g.; remainder 8 s.w.g. Profiles of all welds dressed on completion

1—*Constructional details of 2-weld gusseted bend*



Welding procedure: Root run 12 s.w.g. back chipped; sealing run 12 s.w.g.; remainder 8 s.w.g. Profiles of all welds dressed on completion

2—*Constructional details of 3-weld gusseted bend*

pipe. The highest stresses would naturally be close to the junction between two segments and therefore this test is not strictly relevant to the present investigation. The bend investigated was a four-weld bend made from 12 in. × ⅛ in. stainless steel pipe and had an equivalent radius of curvature of approximately 13⅜ in.

Some fatigue tests on welding elbows and two-weld gusseted bends have been reported by Markl.[3] He draws the conclusion that a welding elbow could safely be stressed to 20% higher than a gusseted bend for bending in the plane of the bend, and 70% higher for out-of-plane bending. In these experiments again no stress measurements were made and they are therefore of limited significance for the present investigation.

Test Specimens

Two types of 90° fabricated bend were tested, of 12 in. nominal bore and ⅜ in. thick. These are shown in Figs. 1 and 2, and are conveniently named 2- and 3-weld bends. The 2-weld bend is made of one full segment, subtending an angle of 45°, and two half segments, each of 22½°. The 3-weld bend is composed of two full segments, each subtending an angle of 30°, and two half segments of 15° each. Both types were designed to have an equivalent radius of 18 in. The equivalent radius describes a circle to which the centre lines of the bend segments are tangential.

The bends were made with great care so as to ensure similarity between the specimens and to permit an accurate definition of the bend geometry. For the 3-weld specimens, of which four were made, the pipe dimensions were sufficiently constant and close to the nominal sizes to permit it to be used as-received. For the three 2-weld bends, however, the pipe to be used was of variable thickness and had to be machined. Final dimensions were 12 in. bore and 0·36 in. thick.

In all cases, the weld reinforcement was dressed off to give a sharp intersection between segments. Gamma-radiographic examination of the 3-weld specimens revealed that the welds were of good quality, with only infrequent evidence of minor porosity. The pipe material was a good quality mild steel having the properties shown in Table I.

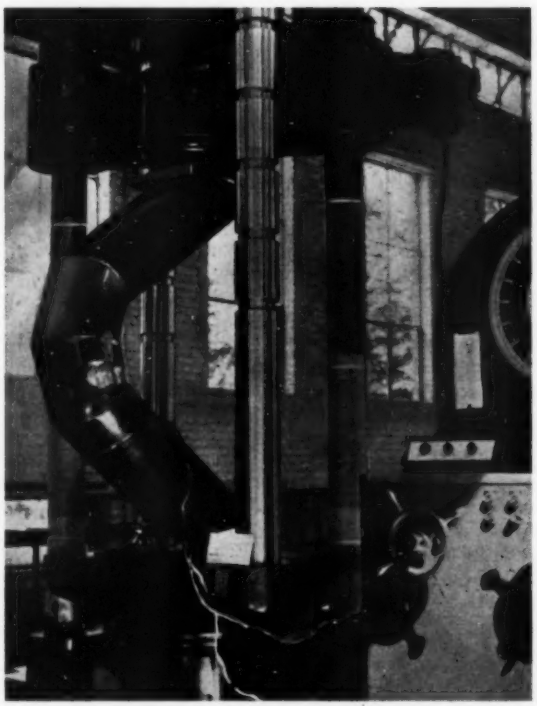
Table I

Mechanical properties of material

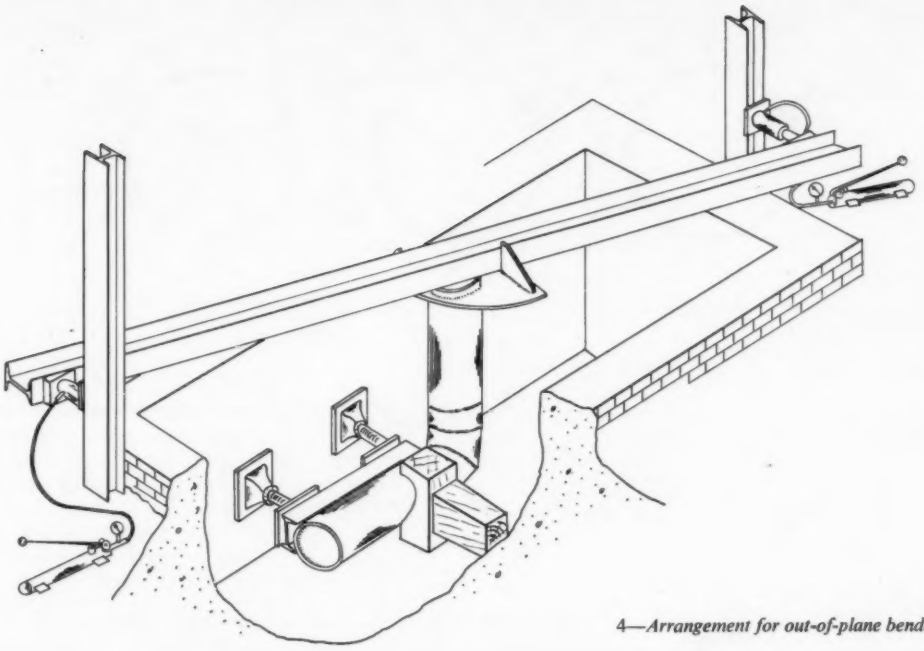
Yield Point	18·2 tons/sq.in.
Ultimate Tensile Strength	27·4 tons/sq.in.
Elongation on 2 in.	37%
Red. in Area	62%

Types of Test

Both 2-weld and 3-weld bends were tested under in-plane bending (*i.e.*, bending moments tending to increase or decrease the bend radius, *see* Fig. 3) and under internal pressure. In a test to verify the principle of superimposition of stresses, a 3-weld bend was subjected to pressure and in-plane bending simultaneously. A 3-weld bend was subjected also to out-of-plane bending, as shown in Fig. 4. For the purpose of these tests, tangent pipes were welded to the bends. The tangent pipes were of the same material and nominal size as the bend, and of length equal to 1½–2 diameters. This length was considered sufficient to prevent any interference with the flexibility and stress system of the bend. It also ensured that the direct stresses due to thrust in the in-plane bending tests were negligible compared with the stresses due to bending. The free ends of the tangent pipes were capped with flat steel plates to which lugs were



3—*3-weld gusseted bend in testing machine*



4—Arrangement for out-of-plane bending

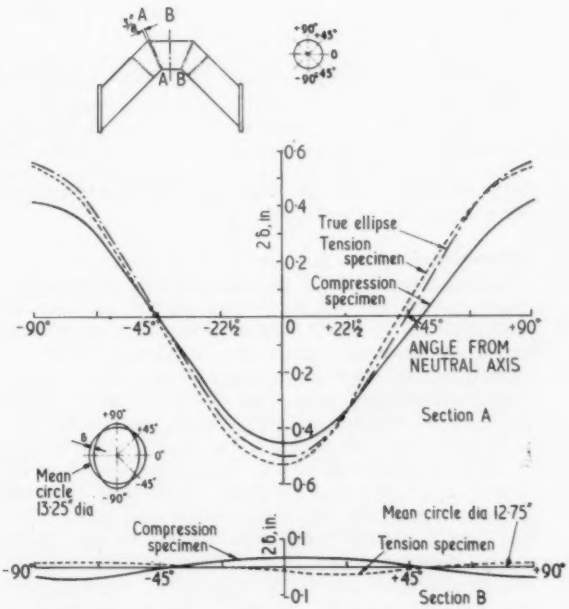
welded, and the specimens were loaded through pinned joints. Thick flat end-plates were used to seal the tangent pipes in the pressure tests that were continued to bursting.

Comprehensive measurements of bend behaviour, as described in the next section, were made during each type of test, both within the elastic range and after the observation of yield. Where possible the tests were continued to failure. In the pressure tests the failure point was bursting, and for the in-plane bending tests tending to reduce the radius of curvature, failure took place by plastic buckling. In the tests tending to increase the radius of curvature no buckling occurred, and the tests were stopped after a large angular bend deflection had been recorded. Owing to limitations of the loading rig used for out-of-plane bending, it was not possible to carry the test much beyond initial yielding. Also, the stresses measured were less than for the same moment applied in the plane of the bend, and for these reasons the 2-weld bend was not subjected to this type of test.

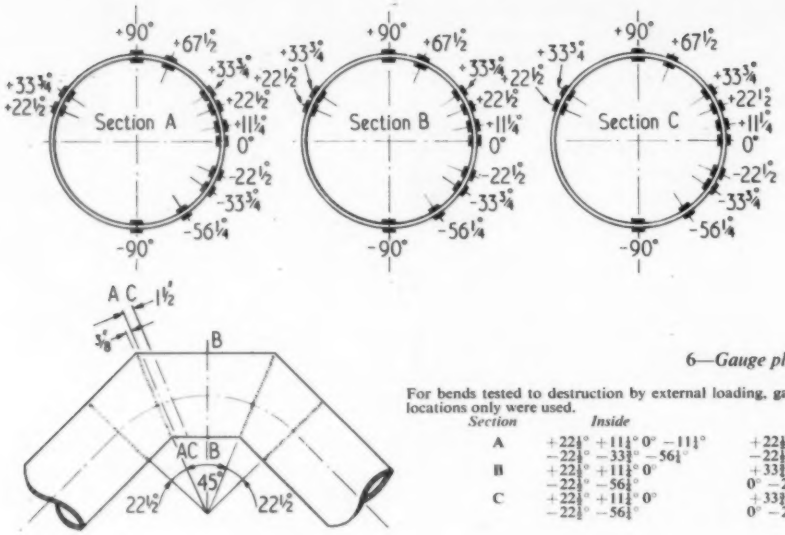
Measurements

Deflections

Measurements of the deflection along the line of the thrusts were made during the in-plane bending tests. These measurements were made by dial gauges attached to both sides of one of the end plates of the specimen. The gauges were connected by fine steel wires to corresponding points on the other end plate. When the deflections became large, the dial gauges were removed and measurements were made in the same locations using a steel rule. Changes in the effective moment arm, measured from the line of the thrusts to the centre of the middle cross-section of the bend, were noted and the applied moment was calculated for each value of load. For the internal pressure test on a 3-weld specimen, the end plates were mounted on rollers to minimise restraint, and measurements of deflection were made using a method similar to that already described. In the out-of-plane bending tests, the deflections were so small that the unavoidable inaccuracies in measurement became a



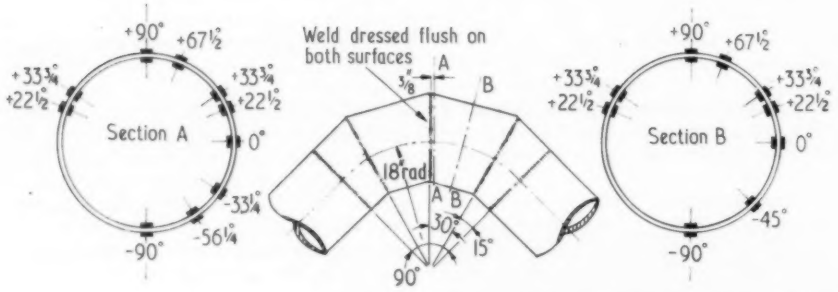
5—Initial shapes of sections of 2-weld bends used for external load tests



6—*Gauge plan for 2-weld bend*

For bends tested to destruction by external loading, gauges at the following locations only were used.

Section	*Inside*	*Outside*
A	+22½° +11¼° 0° −11¼° −22½° −33¾° −56¼°	+22½° +11¼° 0° −22½° −33¾° −56¼°
B	+22½° +11¼° 0° −22½° −56¼°	+33¾° +22½° +11¼° 0° −22½°
C	+22½° +11¼° 0° −22½° −56¼°	+33¾° +22½° +11¼° 0° −22½°



7—*Gauge plan for 3-weld bend*

For bends tested to destruction gauges at the following locations only were used.

Test	*Inside* Sect. A	*Inside* Sect. B	*Outside* Sect. A	*Outside* Sect. B
Compression	0° −56¼° −90°	+22½°	0° −90°	0° +22½°
Tension	0° −56¼° −90°	0° +22½°	0° −33¾° −56¼° −90°	
Pressure	−56¼° −90°	−22½° +33¾°	0° −33¾° −56¼° −90°	

large proportion of the total, and the measurements were therefore abandoned.

Diameters

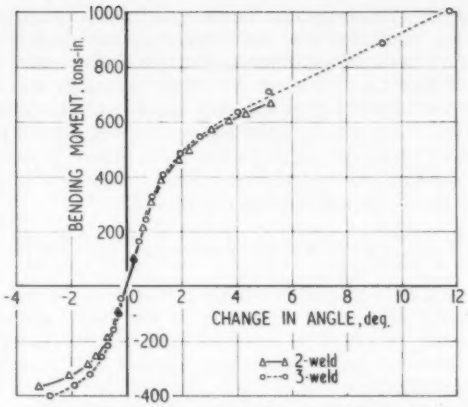
Measurements of the outside diameter of the specimens at two sections were made during the in-plane bending tests for both 2- and 3-weld bends, and also during the internal pressure test on a 3-weld bend. The measurements were made using a large micrometer, fitted with pointed anvils that fitted into small conical depressions in the specimen. Eight diameters were measured on each section. The measurements were made with some difficulty, and the accuracy was of the order of ±0·002 in. However, the measurements of change in diameter for various loads were plotted and the best-fit straight lines were drawn for the elastic range. Figures taken from these lines enabled the changes of diameter to be stated with an estimated accuracy of ±0·0001 in. per 1 ton-in. moment or 100 lb/sq.in. pressure.

The initial deviations of the sections from the true circular or elliptical form are shown in Fig. 5 for two of the 2-weld specimens, and these may be assumed to be typical.

Stresses

Strains were measured on both inner and outer surfaces of the specimens, by means of electric resistance strain gauges. Composite gauges, having two perpendicular elements on one backing paper, were selected because they enabled the measurement of strain in two mutually perpendicular directions to be made at substantially the same location. The use of pairs of single gauges would produce inaccuracies in the deduced stresses owing to the steepness of the strain gradient. Each gauge element had a length of ⅛ in., a resistance of approx. 200 ohms, and a gauge factor of 2·2. The gauges were protected from atmospheric moisture and mechanical damage by a layer of hard paraffin wax, except in the case of the pressure tests for which the internal gauges were waterproofed.

The disposition of the gauges is shown in Figs. 6



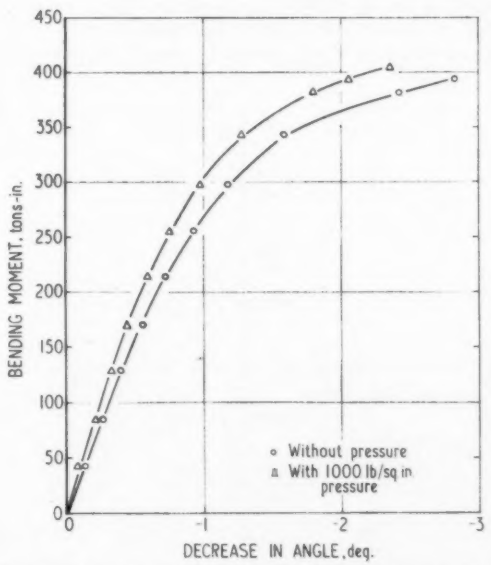
8—*Change of bend angle due to in-plane bending*

and 7 for the 2- and 3-weld bends respectively. It will be seen that measurements have been made on two sections, one *A* being $\frac{3}{8}$ in. away from the intersection of two segments, and the other *B* in the centre of a segment. An additional section *C*, $1\frac{1}{2}$ in. from section *A*, was gauged on the 2-weld specimens. The distance of section *A* from the intersection could not be made smaller owing to the physical size of the gauges. Section *C* was added to the 2-weld bends to provide an indication of the steepness of the strain gradient. Only one specimen of each type of bend was fully strain-gauged, and subjected to all types of loading. The specimens tested to failure were partially gauged as shown in the tables accompanying Figs. 6 and 7.

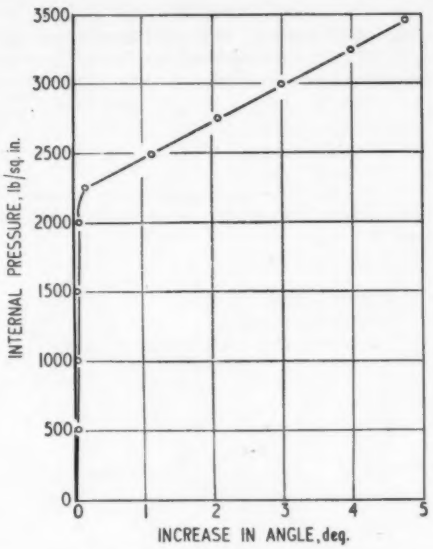
Results

Deflections

The measurements of the change in distance between the end plates of the specimens have been used to calculate the change in angle of the bend. These angular changes are shown in Fig. 8, in which the results for 2- and 3-weld bends are plotted for the in-plane bending tests. Figure 9 shows the effect of a constant internal pressure on the rotation of a 3-weld bend under in-plane bending, and Fig. 10 shows the rotation due to pressure alone.



9—*Rotation of 3-weld bend subjected to in-plane bending*



10—*Rotation of 3-weld bend subjected to internal pressure*

To calculate the change in angle of the bend from the measured deflection, it was necessary to assume that the tangent pipes and bend segments remained straight with all the rotation occurring at the intersections. Further, it was assumed that each intersection contributed the same amount to the total rotation. These rotations are independent of the length of the tangent pipes used in the tests, and are therefore of greater general application than the deflections. However, it is convenient to express the stiffness of gusseted bends in terms of the flexibility factor *K*,

Table II

Flexibility factors for gusseted bend specimens

Type	*Tangent* Length, in.*	*Loading†*	*Δ/W, in./ton*	*Flexibility Factor, K*
2-weld	16·9	T	0·0144	7·70
2-weld	23·2	C	0·0266	8·50
2-weld	25·5	C	0·0320	8·70
3-weld	17·3	T	0·0145	7·45
3-weld	24·7	C	0·0280	8·10
		C + P	0·0249	7·15
3-weld	26·5	T	0·0310	7·85
		C	0·0310	7·85

* Tangent length is the length of straight pipe measured on the pipe centre line between the cover plate and point of tangency with the 18 in. radius bend.

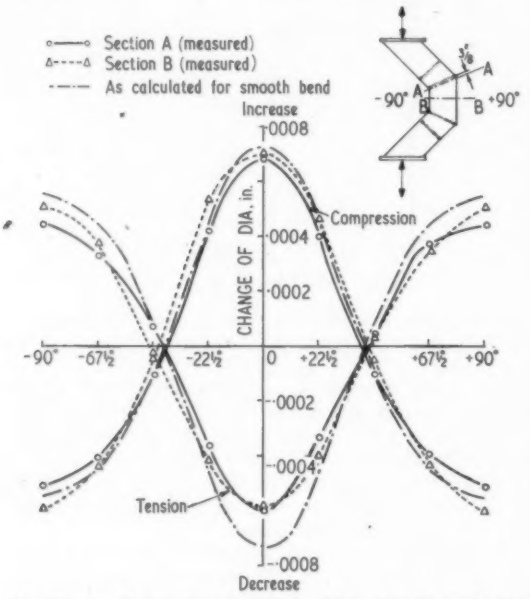
† T = Tension: increasing bend radius
C = Compression: reducing bend radius
P = Static internal pressure of 1,000 lb/sq.in.

Table III

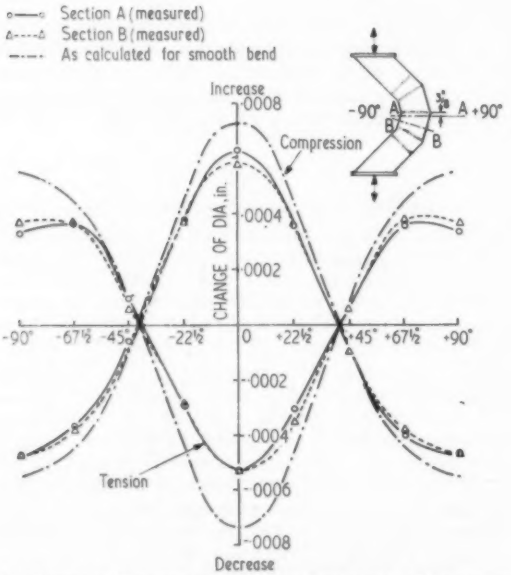
Theoretical (Von Karman) flexibility factors for equivalent smooth bends

Type	*r*, *in.*	*t*, *in.*	*R*, *in.*	λ	*Flexibility Factor*, *K*
2-weld	6·18	0·36	18	0·170	10·22
3-weld	6·188	0·375	18	0·177	9·84

r = mean radius of pipe section *R* = equivalent bend radius
t = thickness $\lambda = tR/r^2$



11—*Change of diameters around 2-weld bend under an in-plane bending moment of 1 ton-in.*



12—*Change of diameters of 3-weld bend under an in-plane moment of 1 ton-in.*

used for smooth bends. To do this, it is necessary to assume that the fabricated bend is replaced by a smooth bend of the same equivalent radius and then to deduce the value of K which accounts for the observed deflection of the gusseted bend. The deflection of a smooth bend of radius R fitted with equal tangent pipes of length L, due to a thrust W acting along a line joining the free ends of the tangent pipes, is given by the well-known expression:

$$\Delta = \frac{W}{12EI}\left\{4L^3+KR\left[3\pi L^2+6(4-\pi)LR+6(\pi-3)R^2\right]\right\}$$

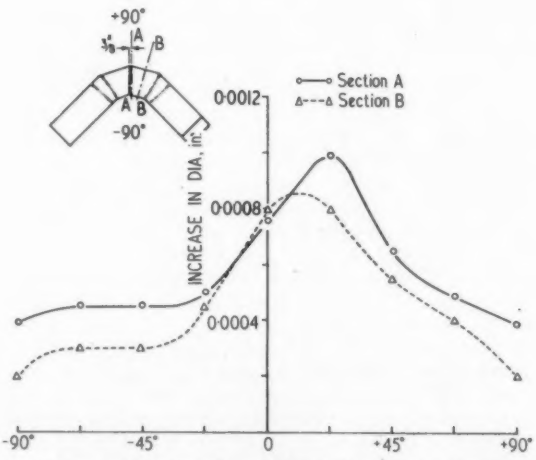
where K is the bend flexibility factor, I is the second moment of area of a section of the bend about a diameter, and E is the modulus of elasticity. Taking E as 13,400 tons/sq.in., and mean values of Δ/W from the thrust/deflection curves drawn for the tests within the elastic range, the flexibility factors were calculated and are shown in Table II. Flexibility factors calculated for the equivalent smooth bends, using three terms in Von Karman's analysis, are shown in Table III.

Diameters

The changes in diameter for the in-plane bending tests are shown in Fig. 11 (2-weld) and Fig. 12 (3-weld), for an applied moment of 1 ton-in. For comparison, the change in diameter of a smooth bend having the same equivalent radius has been calculated using Von Karman's analysis, taking three terms in the series for the deformation. This change is also plotted in Figs. 11 and 12. The changes in diameter that occurred during the internal pressure tests of the 3-weld bend are shown in Fig. 13, for a pressure of 100 lb/sq.in.

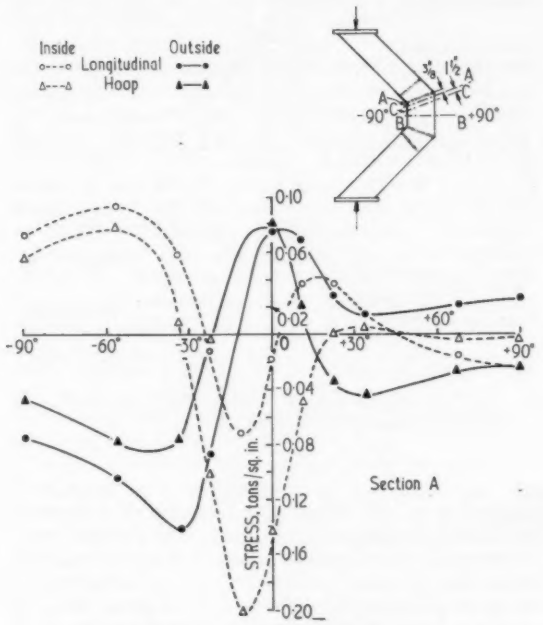
Stresses

The strain readings of each gauge were plotted against applied moment (or pressure) and the best-fit straight lines were drawn in the elastic region. The slopes of these lines were used to deduce the stresses acting in the directions of the gauge elements at unit moment (or pressure), using the usual formulae for a biaxial stress field. It should be noted that these are

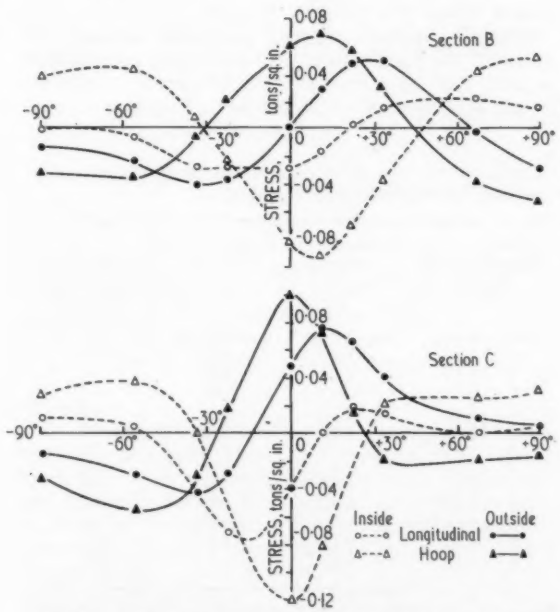


13—*Change of diameter of 3-weld bend under an internal pressure of 100 lb/sq.in.*

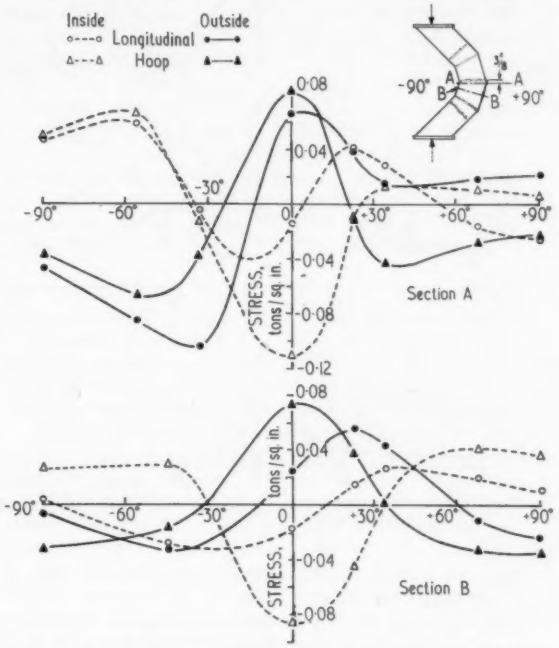
not necessarily the principal stresses acting, but the stresses normal and parallel to the section considered. For the sections *B*, these directions coincide with the longitudinal and hoop direction of the pipe, whereas for sections *A* (and *C* on the 2-weld bends) they are displaced from these directions by an angle varying from 0° to 22½° for the 2-weld bends, and 0° to 15° for the 3-weld bends. For convenience, they are termed longitudinal and hoop stresses in the diagrams. Figure 14 shows the distribution of these stresses on section *A* and Fig. 15 on sections *B* and *C*, for an in-plane moment of 1 ton-in. acting on a 2-weld bend. Similar results are shown for the 3-weld bend in



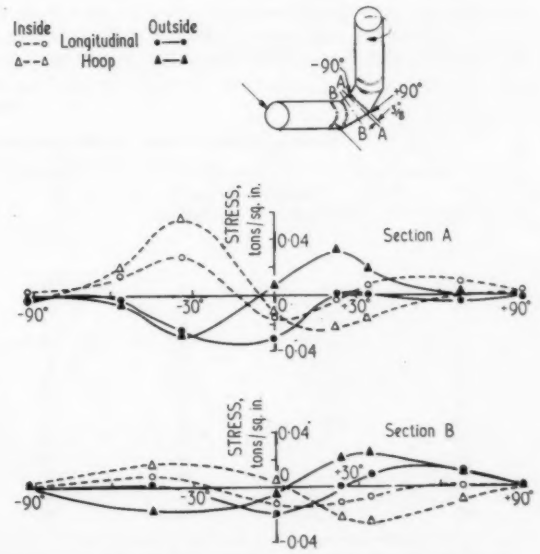
14—*Stress distribution around 2-weld bend subjected to an in-plane (compressive) bending moment of 1 ton-in.*



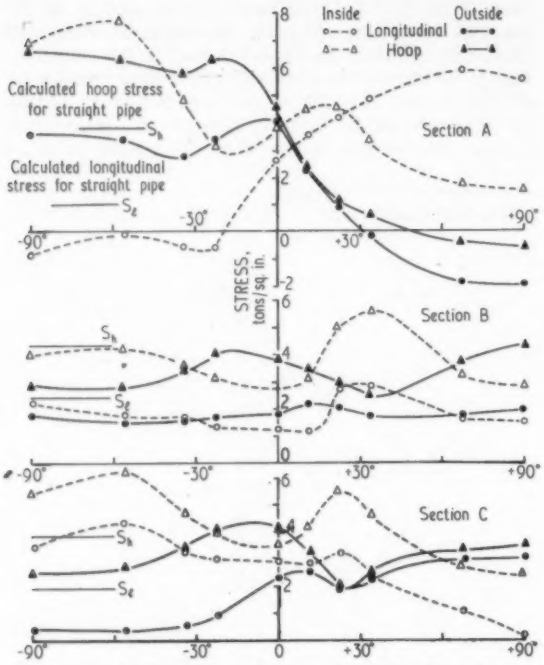
15—*Stress distribution around 2-weld bend subjected to an in-plane (compressive) bending moment of 1 ton-in.*



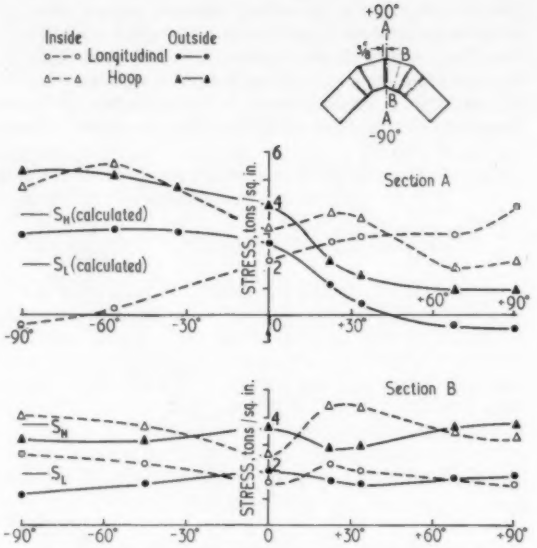
16—*Stress distribution around 3-weld bend subjected to an in-plane (compressive) bending moment of 1 ton-in.*



17—*Stress distribution around a 3-weld bend subjected to an out-of-plane bending moment of 1 ton-in.*



18—*Stress distribution around a 2-weld bend subjected to an internal pressure of 500 lb/sq.in.*



19—*Stress distribution around a 3-weld bend subjected to an internal pressure of 500 lb/sq.in.*

Figs. 16 and 17, the latter showing stresses due to out-of-plane bending.

The stresses due to an internal pressure of 500 lb/sq.in. are shown in Figs. 18 and 19, for 2- and 3-weld bends respectively.

The stresses due to in-plane bending, shown in the graphs, are average values for three specimens, for both types of bend. For this purpose, where necessary, the signs of the stresses were reversed to give values due to a moment tending to reduce the bend radius. The greatest deviations from the stresses averaged in this way were ±5%, indicating a high degree of consistency in the experimental results. The stresses due to internal pressure were averaged in a similar way.

The results of the check on the principle of superimposition of stress systems are shown in Table IV. Values of measured stresses due to an in-plane bending moment and internal pressure are given separately, and their sums compared with the stresses measured when both forms of loading were applied simultaneously.

Tests to failure

Under in-plane bending moments tending to decrease the bend radius, a 2-weld specimen showed general yield at a moment of 189 tons-in., and collapsed at a moment of 376 tons-in. The distortion of

Table IV

Stresses measured under internal pressure and external loading separately and together

Angle	*Gauge Location* Surface	Section	Longl. or Hoop	*Stresses, tons/sq.in.* External Load Only* (*a*)	Internal Pressure Only† (*b*)	(*a*)+(*b*)	External Load and Internal Pressure Simultaneously
0	IN	A	H	C −8·66	3·36	−5·3	−4·3
−90	IN	A	H	C 3·15	5·45	8·6	8·1
22½	IN	B	H	C 1·25	1·74	3·0	3·1
0	OUT	A	H	C 6·15	3·45	9·6	9·2
22½	OUT	B	L	C 4·10	2·02	6·1	6·1
0	OUT	B	H	C 5·51	3·67	9·2	8·9
0	IN	A	H	T 3·57	3·36	6·9	6·5
−90	IN	A	H	T −1·19	5·63	4·4	4·9
0	IN	B	H	T 2·51	3·24	5·8	5·1
22½	IN	B	H	T 1·10	4·24	5·3	5·0
−33½	OUT	A	L	T 2·65	3·36	6·0	6·2
−33½	OUT	A	H	T 0·80	5·17	6·0	5·6
−90	OUT	A	H	T 1·05	5·06	6·1	6·0

* Where stress is marked *C* external load was such as to produce a moment of 75 tons-in. tending to reduce the radius of curvature. Where stress is marked *T* the moment was 28·5 tons-in. tending to increase the radius of curvature.

† The internal pressure was 500 lb/sq.in. in all cases.

section *B* was measured after the specimen had been removed from the testing machine, and the diameter in the plane of the bend was found to be 9·38 in., whilst in the perpendicular direction it was 15·69 in. No cracking was found in either the segments or the welds. The 3-weld bend yielded at 165-tons-in. and collapsed at 412 tons-in., again with no sign of cracking. The diameters measured at section *B* after collapse were 10·63 in. and 15·13 in.

In-plane bending tending to increase the bend radius produced yielding at 159 tons-in. in the 3-weld specimen. The maximum moment applied was 1,045 tons-in. at which load the change in bend angle was approximately 12°, and the test was discontinued. No cracking was observed, and the diameters of section *B* measured after the test were 14·50 in. (in the plane of the bend) and 10·25 in. (in the perpendicular direction). For the 2-weld specimen, part of the loading gear failed at an applied moment of 740 tons-in., but as the change in bend angle was already large (6°), no attempt was made to continue the test. The diameters measured at section *B* were 13·69 in. and 11·38 in.

The loading rig used in the out-of-plane bending test on the 3-weld bend was limited in capacity to 595 tons-in., and this moment produced no pronounced permanent set in the bend.

Under a steadily increasing internal pressure, the 2-weld bend showed general yield at 1,800–1,950 lb/sq.in. and burst at 2,900 lb/sq.in. The fracture was a shear failure 6 in. long, lying in the longitudinal direction across a welded intersection and located at about 45° to the inner arc of the bend.

In the case of the 3-weld bend, yield was first observed at 1,000 lb/sq.in. and bursting took place at 3,700 lb/sq.in. The failure was brittle and extended along the whole of the inner arc of the bend.

The bursting pressures of straight pipe calculated by means of the mid-diameter formula and U.T.S. from Table I, were 3,560 lb/sq.in. for the average dimensions of the pipe used for the 2-weld bend and 3,720 lb/sq.in. for the 3-weld bend.

Discussion

The large number of experimental observations included in the results can be interpreted best in comparison with the behaviour of smooth bends of equivalent radius. As will be seen, the comparison is sufficiently close to lend support to the use of the well-established smooth-bend theory for the design of gusseted bends, and provides a close indication of the differences in behaviour that would have to be accounted for in any theory for fabricated bends.

Flexibility

The enhanced deflection under in-plane bending loads of a pipe bend, above that predicted by ordinary beam theory, is expressed customarily by means of the flexibility factor *K*. Comparisons between the flexibility of the gusseted bends and smooth bends of equivalent radius are made in Tables II and III. The experimentally deduced values of *K* for the 2-weld bends fall in the range 75–85% of the smooth bend value of 10·22, whilst those for the 3-weld bends occupy a range (excluding the test in which an internal pressure was present) of 76–83% of the smooth bend value (9·84). The lower values of flexibility factor are associated with the specimens having shorter tangent pipes, and it is probable that the rigid end-plates restrained the tendency of the bend sections to assume an oval shape, thus limiting the flexibility. Nevertheless, the comparison is sufficiently close to justify the use of smooth-bend theory in calculating the flexibility of fabricated bends. This is in accordance with the findings of Gross and Ford in their work on a 4-weld gusseted bend.

The effect of an internal pressure of 1,000 lb/sq.in. on the flexibility of a 3-weld bend was to reduce the flexibility factor of 8·10, determined without pressure acting, by 12% to 7·15. Rodabaugh and George[4] have developed a theoretical expression for the flexibility factor of smooth bends when pressurised, and this predicts a reduction in flexibility factor of 16% for the smooth bend equivalent to the present 3-weld bend.

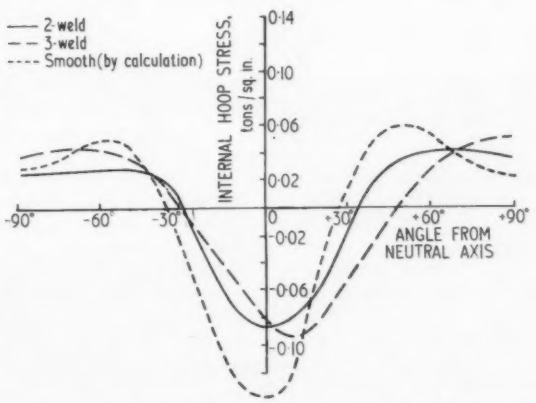
Diameters

The measured changes under in-plane bending, in the diameter of sections close to and remote from the welded joints, are compared in Figs. 11 and 12 with those calculated for a smooth bend. Agreement is good, and moreover the distortions of both sections are almost identical. This supports the earlier contention, made in deducing angular movements from deflection data, that rotation in fabricated bends is generated mainly at the intersections, with the straight sections remaining substantially straight. The agreement with smooth-bend theory for diameter changes does not necessarily imply that the stress systems should be identical since local stresses, particularly of a bending nature, could exist without producing significant diametral deformation.

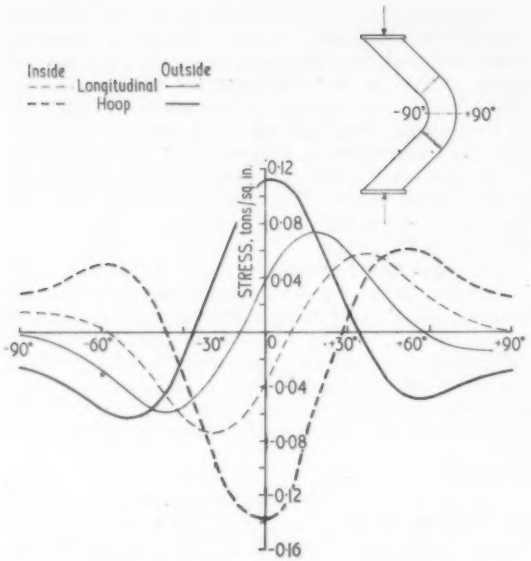
Stresses

In-plane bending

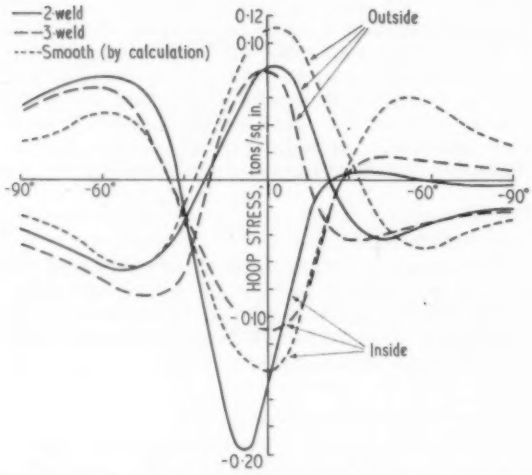
The stresses at sections *B* (remote from welds) are, in both the 2-weld and 3-weld bends, of similar distribution to, but of smaller maximum value than, those of a smooth bend. The internal hoop stress, which has the highest peak, is shown in Fig. 20 for



20—*Comparison of internal hoop stress at section B for 2-weld, 3-weld, and smooth (by calculation) bends subjected to an in-plane bending moment of 1 ton-in.*



21—*Calculated stress distribution in a smooth bend subjected to an in-plane (compressive) bending moment of 1 ton-in.*

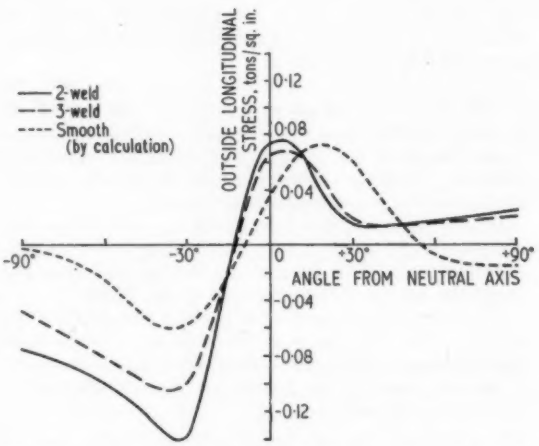


22—*Comparison of hoop stress at section A for 2-weld, 3-weld, and smooth (by calculation) bends subjected to an in-plane bending moment of 1 ton-in.*

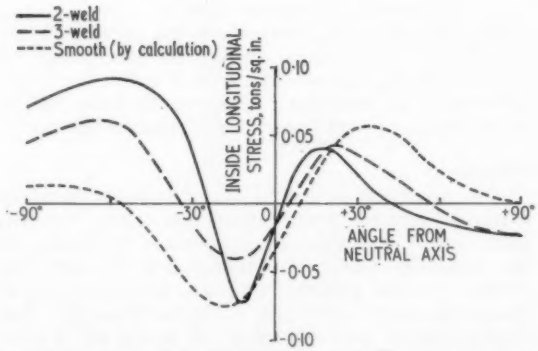
fabricated and smooth bends, to illustrate this point. The complete theoretical stress distribution for a smooth bend of 18 in. radius in 12 in. bore pipe 0·375 in. thick is given in Fig. 21.

It is close to the welded intersections that large differences between stresses in smooth and fabricated bends occur. The hoop stresses at inner and outer surfaces at sections *A* (close to the welds) for 2-weld and 3-weld bends are compared with one another and smooth-bend theory in Fig. 22. For the 3-weld bend, the maximum measured hoop stress of −0·11 tons/sq.in. per ton-in. moment (close to the neutral axis, at the inner surface) is still less than the maximum of −0·138 tons/sq.in. predicted for a smooth bend. However, for the 2-weld bend, the measured peak stress was −0·20 tons/sq.in. and therefore 45% greater than for the smooth bend.

A more marked difference in the character of the stress distribution in fabricated bends is evident when the longitudinal stresses at the intersection are examined. The comparison with smooth-bend theory is made in Fig. 23 for the outer surface and in Fig. 24 for the inner surface. The symmetry between extrados and intrados regions, present in smooth bend stresses is lost and whilst for the extrados (0° to +90°) there is still fair similarity of stresses for fabricated and smooth bends, on the other side of the neutral axis (0° to −90°) it is clear that a large component of bending stress has arisen in the fabricated bends. If the direct longitudinal stress acting in the fabricated bends is calculated as the mean of the stresses at the inner and outer surfaces, it is in fair agreement with that for the smooth bend. Accordingly, the significant difference between fabricated and smooth bends is the appearance of a longitudinal bending stress,



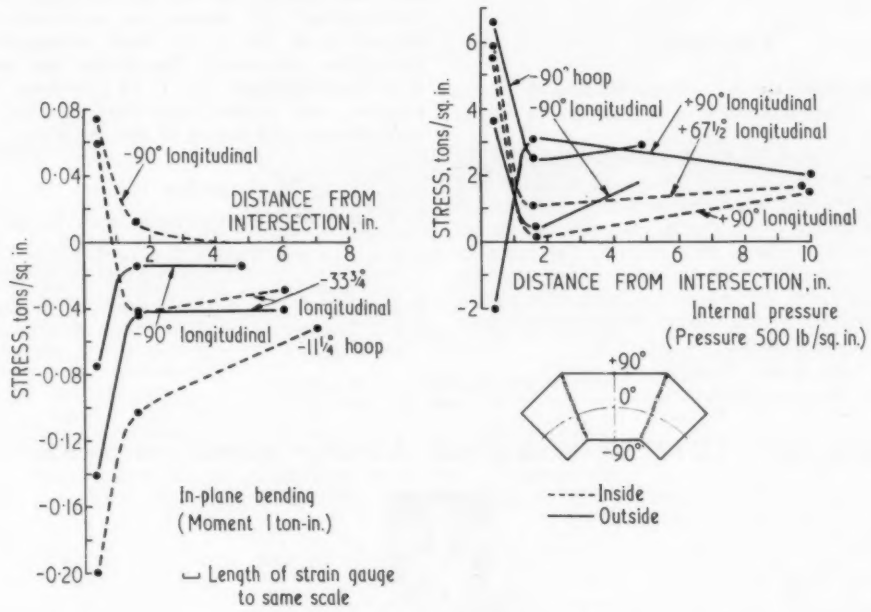
23—*Comparison of outside longitudinal stress at section A for 2-weld, 3-weld, and smooth (by calculation) bends subjected to an in-plane bending moment of 1 ton-in.*



24—*Comparison of inside longitudinal stress at section A for 2-weld, 3-weld, and smooth (by calculation) bends subjected to an in-plane bending moment of 1 ton-in.*

compressive at the inner surface, in a region close to the welded joints and having a maximum value at a point between the neutral axis and the inner arc of the bend.

This additional bending stress is larger for the 2-weld bend but nevertheless is exceeded by the hoop stress near the neutral axis already noted. Since differences between smooth and fabricated bend stresses must be associated largely with the discontinuity in profile at the joints of fabricated bends, it is probable that higher stresses exist at points closer to the intersections than could be reached using strain gauges of finite size. The point is well-illustrated in Fig. 25, where stresses at points 0·375 in., 1·875 in., and remote from an intersection are plotted and in Fig. 17 must be interpreted in this light. It may be assumed that the torsion loading gave rise to only small and reasonably uniform stresses, so that the stresses plotted could be safely attributed to the out-of-plane bending component. This component has a value of $1/\sqrt{2}$ the applied couple and therefore, for comparison with the stresses produced by an in-plane moment of 1 ton-in., the stresses in Fig. 17 should be multiplied by the factor $\sqrt{2}$. The inside hoop stress then has a maximum value of 0·078 tons/sq.in. per ton-in. out-of-plane moment, and this may be compared with the maximum value of 0·11 tons/sq.in. per ton-in. in-plane moment (Fig. 16, section *A*). This is in accordance with the smooth-bend theory, in which stresses due to



25—Significant stress gradients in a 2-weld bend

joined by tentative curves. The stress gradient at the points of stress measurement nearest the intersection is high and it is doubtful whether maximum stresses have been measured. An attempted resolution of this difficulty using strain gauges of smaller size would probably lead only to inconclusive results, and instead it is suggested that a theoretical analysis designed expressly to account for the divergence in smooth and fabricated bend behaviour should be sought.

Out-of-plane bending

In the method of loading adopted for the out-of-plane bending test on a 3-weld bend, a turning moment was applied to one tangent pipe, while the other was prevented from moving. The type of loading varied around the bend, therefore, from a pure torsion at one end to a pure out-of-plane moment at the other end. At the section *A*, where stress measurements were made close to the intersection at the midpoint of the bend, the loading was an equal combination of torsion and out-of-plane bending, and the stress distributions in-plane bending exceed those due to out-of-plane bending.

Internal pressure

For internal pressure, the maximum proportional increase over the calculated stress in a straight pipe of the same dimensions is in section *A* at −56°, where the hoop stress in the 2-weld bend (Fig. 18) is double the calculated hoop stress for a straight pipe. In a smooth bend, the maximum stress concentration for internal pressure is usually not more than 1·1. It is interesting to observe the very pronounced similarities between the measured stress distributions for the 2-weld bend and those given in Fig. 19 for the 3-weld bend.

Further Work

In the previous section, attention has been drawn to the fact that, with the strain gauge technique at present used, it is likely that the measured stresses

underestimate the maximum stresses in the gusseted bends. For this reason it is suggested that a continuation of the experimental stress analysis work would not be fully rewarding. Nevertheless, the present experimentally measured stresses might prove of value if they could be used to check the accuracy of a theoretical prediction of the stress distribution. As yet no such theoretical prediction is available, but it is suggested that attempts should be made to obtain an approximate solution of the problem.

Immediately useful results would be obtained from a series of fatigue tests on fabricated bends, and such a programme has been started at B.W.R.A. Earlier fatigue tests[2] on smooth bends have been carried out to provide a reference level with which to compare such tests.

Conclusions

(1) The flexibility under in-plane bending of 2-weld and 3-weld gusseted bends is closely similar to that of smooth bends.

(2) Stresses in gusseted bends are of similar distribution to those in smooth bends, with the addition of a longitudinal bending stress component in the vicinity of the intersections. In magnitude, the maximum measured stress in the 2-weld bend is 1·4 times as great as in a smooth bend, although it is possible that the true maximum stress has not been discovered. Measured stresses in the 3-weld bend are less than those in the 2-weld bend.

(3) The welded joints do not constitute a weakness in gusseted bends tested to destruction under static external loading or internal pressure.

(4) The stresses acting in a gusseted bend, subjected to simultaneous external load and pressure, are the sums of those stresses acting when the bend is subjected to each form of loading separately.

(5) The flexibility of gusseted bends is diminished by the presence of internal pressure by an amount similar to that evaluated for smooth bends.

Acknowledgments

The Association is grateful to Stewarts and Lloyds Ltd. who carried out the radiography. The authors acknowledge with thanks the assistance they have received from Dr. A. A. Wells, Assistant Director, throughout this work. Thanks are due also to Mr. R. F. Quartermaine, Mr. E. O. Lawrance, Mr. G. A. Higgins, and others who have assisted with the manufacture and testing of the specimens.

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THE INSTITUTE OF WELDING – SOUTH LONDON BRANCH

CONVERSAZIONE & EXHIBITION

devoted to

"QUALITY CONTROL IN WELDING"

on

WEDNESDAY, JUNE 14th, 1961

(3 until 8 p.m.)

at

54 PRINCES GATE, LONDON S.W.7.

An opportunity for discussion and display of defects in welds and methods for their detection. 20 exhibitors.

Tea provided

All members and their guests are welcome

NEWS and Announcements

News of the Institute and Branches
B.W.R.A. · Other Societies ·
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INSTITUTE ACTIVITIES

MEETINGS

Spring Meeting 1961

The Spring Meeting of the Institute, held in London on **25th–28th April**, although it did not attract as large a number of enrolments as might have been expected during the Engineering, Marine, Welding and Nuclear Energy Exhibition at Olympia, produced some very lively discussion and appeared to be enjoyed by those who took part.

The proceedings opened with the Fourth Annual Lecture on "*Welding in the Non-Ferrous Field*", by Dr. N. P. Inglis, with Mr. Gilbert Roberts in the chair because of the regretted absence of the President, Mr. E. Fuchs, for reasons of health.

There was a small reception at the Institute after the lecture from which members of the Council withdrew in order to entertain the lecturer at dinner.

The first of the three technical sessions on the following morning attracted a large audience for the presentation of four papers:

"*Mild Steel Electrodes for Contact Welding*" by J. W. Addie

"*Mild Steel Welding with Argon/CO_2 Mixtures*" by B. H. Baker, N. Crevis, J. A. Lucey and F. W. Lunau

"*Automatic Tungsten Arc Welding of Heat Exchangers*" by N. T. Burgess

"*Inert-Gas Shielded Metal-Arc Welding of Thick Copper without Preheat*" by K. J. Clews

On the same afternoon a party of 25 visited the Welded Maidenhead Bridge where they were received by Mr. K. A. Goodearl, Site Engineer.

The visitors were given a most interesting "tour" of the bridge, including the inspection of the anchor links and a view from a motor launch of the underside of the main girders.

Many problems associated with a welded construction of this nature were discussed with the engineers who acted as guides. A vote of thanks to them was proposed by Mr. N. L. G. Lingwood.

On Thursday morning the following papers were presented and discussed:

"*Developments in Stud Welding*" by D. J. N. Laurie

"*Foil Seam Welding*" by Dr. Ing. F. Busse

"*Some Recent Developments in Brazing*" by G. R. Bell

In the evening the London Branches held their Joint Annual Dinner at Quaglino's where the attendance exceeded 400. The Chair was taken by Mr. R. W. Ayres, retiring President of the North London Branch and the principal guest who proposed the toast of "The Institute and the London Branches" was Sir Kenneth Hague. The response was made by Mr. Harry West, Vice-President of the Institute. The Chairman proposed the toast of "The Guests" to which Professor A. J. Kennedy replied, asking for the support of the Institute and its members for the Post-Graduate Course in Welding Technology which is to open at the College of Aeronautics, Cranfield, in the Autumn.

The third and last session was held on the following morning, when the papers given below were presented and discussed:

"*Auto-Pneumatic Metal Spraying Machines*" by W. E. Stanton

"*A Note on Flame Spraying Developments*" by Lloyd Manuel

"*An Adhesion Test for Aluminium Spray Coatings and Other Metallised Surfaces*" by R. S. Smith and N. Stephenson

This was the day of the Institute's official visit to the Engineering Exhibition. Some twenty members of the Council were guests of the exhibition organisers at luncheon at Olympia, and the Institute was represented at the top table by Mr. R. G. Braithwaite, a Past-President.

Welding in Shipbuilding

A very large attendance is expected at the Autumn Meeting of the Institute this year, which will be held in London between **30th October** and **3rd November, 1961**, and will take the form of a Joint Symposium on "*Welding in Shipbuilding*".

Already more than thirty papers from nine countries have been promised. These will be preprinted in a separate volume and subsequently reprinted with a report of the discussion.

The Annual Dinner, for which those enrolled for the Symposium will be able to purchase tickets, is to be held at the Connaught Rooms on the evening of **Wednesday, 1st November.**

All technical sessions will of course be open to members, who will also be able to enrol for the Symposium and, if they wish, to obtain copies of the preprints and proceedings and to attend certain other social functions arranged in connection with it.

ELECTION OF MEMBERS

The following elections have been made effective from the dates indicated: 15th December 1960—

Associate

S. K. Lala (India)

26th January 1961—

Companion

C. Bennett (Staffs.).

Graduate

S. C. Bhalla (India).

23rd February 1961—

Member

*H. C. Constantine (Banbury).

Companions

S. Blake (Warwickshire); E. L. Courtney (Bedford); R. M. Wisdom (Worcestershire); G. A. Reston (Lancs).

Graduate

S. K. Mazumder (Germany).

Associates

L. F. Axford (Dorset); P. K. Bhattacharjee (India); E. B. H. Devine (Kent); J. P. Marshall (Birmingham).

23rd March 1961—

Members

F. B. Bott (Shropshire); M. Riddihough (Solihull); E. Griffin (Birmingham).

Associate Members

R. S. Brunton (Glamorgan); J. M. Francis (Gourock); W. F. Robertson (Glasgow); P. R. Webster (Japan); T. W. Eadie (Derby); J. Peplow (Stockton on Tees).

Companions

S. K. S. Pratury (India); F. H. Scothern (Doncaster).

Graduate

J. T. Gibbin (Yorks.).

Associates

O. E. Clark (Hants); W. Cox (Cheshire); J. J. Higgins (Middlesex); K. Hunter (Co. Durham); F. Hutchinson (Warwickshire); H. Marsden (Co. Durham); R. M. Patrick (Nigeria); A. A. Twine (Hants); S. I. Varadachari (India).

* Transfer from Associate Member

NEWS OF MEMBERS

Mr. **A. McNabb**, A.M.I.Mech.E., has left Dewrance & Co. Ltd. to become Assistant Works Manager with James F. Low & Co., Monifieth, Angus.

Mr. **E. N. Farrar**, A.M.I.Mech.E., M.I.Prod.E., who has been with Davey Paxman & Co. Ltd. for many years, has now taken over the post of Industrial Relations Manager of that Company.

Mr. **H. W. G. Hignett** has been appointed a Director of the International Nickel Company (Mond) Ltd. He remains Managing Director of Henry Wiggin & Co. Ltd.

Obituary

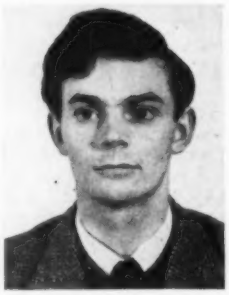
The Council records with regret the death of the following member:

Mr. **A. T. Willmouth** (Birmingham Branch, Associate Member 1941).

CONTRIBUTORS TO THE JOURNAL

J. A. Greenwood, Ph.D., is at the Hinxton Hall Laboratories of Tube Investments Ltd. He read mathematics at the University of Cambridge and then joined Dr. F. P. Bowden's laboratory to do experimental research on the mechanism of sliding friction, for which he received a Ph.D. in 1957.

During this period, as the only mathematician within call, he became involved in theoretical work on the role



of elastic hysteresis losses in rolling friction, and also on the heating of electric contacts. The latter involved the study of contact resistance, current distribution, and heat flow, and has, of course, a close relation to spot welding. In 1957 Dr. Greenwood joined Tube Investments Research Laboratories as a member of the team studying the fundamentals of resistance welding, and since then has been trying to explain spot welding in terms of electric contact theory.

BRANCH NEWS

East of Scotland — **Discussion**

On 20th February a joint meeting was held with the Dundee Branch of The Institution of Engineering Inspection, at the Royal British Hotel, Dundee.

Films loaned by the British Oxygen Co. Ltd., depicting some of the inert-gas and oxy-acetylene welding processes, were shown as a basis for discussion. Questions on many points raised were dealt with by Mr. D. Taylor, Sales Representative for British Oxygen Co. Ltd. to whom the Institutes are indebted for arranging what was a most interesting and instructive evening.

R.D.B.

Manchester — **Quality Control**

On Wednesday 1st March 1961, a lecture entitled "*Quality control methods adopted by a large user when purchasing welded equipment*" was given by Mr. W. Ashworth, A.M.I.Mech.E., assisted by Mr. R. Partridge. The joint author of the paper, Mr. E. Fuchs, M.A., President of the Institute of Welding and a member of the Manchester Branch, was unable to be present. The lecture was fully reported in the October 1959 issue of *BWJ*, and was presented at the Autumn Meeting of the Institute in November of that year.

A.C.M.

Annual Dinner

Nearly 200 members and guests assembled for the Manchester and District Branch Annual Dinner, held at the Grand Hotel, Manchester, on Monday, 6th March 1961, the Chairman of the Branch, Mr. E. H. Lee, presiding.

The Toast of the City and Ports of Manchester was proposed by Mr. C. A. J. Begg, Engineering Director of I.C.I. Ltd., Alkali Division, and the response was made by the Right Worshipful the Lord Mayor of Manchester, Alderman Arthur Donovan, J.P. Mr. C. H. Flurscheim, Director and Chief Electrical Engineer of A.E.I. (Manchester) Ltd., proposed the Toast of the Institute and Branch, to which Mr. Gilbert Roberts, Vice-President of the Institute replied in the regrettable absence through illness of Mr. E. Fuchs, President.

The third Toast was that of the Visitors and Kindred Associations, proposed by the Chairman, to which Dr. C. Whitworth, Principal of the Royal Technical College, Salford, replied.

Mr. Roberts dwelt on the long standing interest of the Branch and of the Manchester College of Technology in advanced instruction in welding and appealed to the industrialists present to support the post-graduate course at the College of Aeronautics, Cranfield, by sending suitable members of their staff to it.

Dr. Whitworth also appealed for industrial support for the courses in welding technology offered at the Royal Technical College, Salford.

N.E. Tyneside — **Annual Dinner**

The Annual Dinner of the Branch was held at the Coatham Hotel, Redcar, on Friday, 3rd March 1961, the Branch Chairman, Mr. M. E. Pool, presiding over a good attendance of some 80 members and guests.

The Toast of the Institute was proposed by the Chairman, and the response was made by the Secretary of the Institute, Mr. G. Parsloe, in the unavoidable absence through illness of the President, Mr. E. Fuchs, and of Mr. R. E. G. Weddell, a Past-President, who had promised to replace the President.

Mr. Parsloe spoke of the importance of advanced education in welding technology, with particular reference to the post-graduate course to be opened at the College of Aeronautics, Cranfield, in October.

The other speaker was Mr. R. A. Exton, Chairman of the Tyneside Branch, who proposed the health of the Chairman.

North London — Unusual Applications

The title of the lecture on 22nd March, "*The design of resistance welding equipment for unusual applications*" was well chosen by Mr. J. D. Morris, who opened by discussing the application of welding to the fabrication of dental braces. Special machines have been designed for the welding of these intricate stainless steel wires, plates, etc. It would appear that, apart from the large dental hospitals, many private practitioners are now becoming welding technicians by carrying out this specialist work at their own premises.

The next subject dealt with was the closing of steel tubes by resistance heating and hydraulic forming. A film was shown illustrating the automatic installation for the production of high-pressure gas cylinders.

The lecturer then discussed the increased use of resistance welding in the electrical component field. This form of welding is replacing soldering to some large degree for the joining of copper to copper, and to many other materials. Again, a short film showed the operation of a special machine designed for the fabrication of a complete coil assembly. A further illustration of special equipments was displayed by a short film showing a machine designed to fabricate bird cages automatically. The purpose of this machine is to save labour costs and general handling time.

The last subject dealt with was the flash-butt welding of carbon steel strip for continuous coils.

The lecture was well received and a lively discussion followed with all questions being very ably dealt with by the speaker.

F.W.C.

South London — Weld Testing

On Wednesday, 15th March, Mr. C. Atkinson, of the National Boiler and General Insurance Co. Ltd., spoke to some 60 members and guests on the subject of "*Testing, inspection and assessment of welds*".

In a very interesting discourse, fully illustrated with several dozen slides, Mr. Atkinson first emphasised the difference between good and bad welds, mentioning some of the contributory causes such as bad fit-up, etc. He mentioned the various categories of weld classification and further dealt with the important topic of welder qualification. He noted how important it was to apply the correct preheat to a weld to achieve sound metallurgical quality and also spoke on the destructive methods of testing, making particular reference to bend tests. He explained that practically all the defects he had shown in the slides were of bad welds but that nevertheless these were of production welds not specially prepared for lecture purposes.

In a lively discussion questions were directed to the rewelding of certain jobs in an attempt to effect a satisfactory repair, and seal welding. Mr. O. Bondy proposed a vote of thanks for what was generally agreed as being a most entertaining and well informed lecture.

N.T.B.

Medway Section — Transportation Pipelines

On 22nd February 1961, at the King's Head Hotel, Rochester, a Joint Meeting with the Institute of Petroleum took the form of a 'Symposium on Pipeline Construction'.

The paper '*Transportation pipelines*' by R. J. Wright, A.M.Inst.W., M.S.M.A., was read by Mr. Page, and gave the history of pipelines from the bamboo pipes used by the Chinese to the present day. Also discussed were the various welding techniques employed, with particular reference to the 'stove pipe' method.

After a short interval for coffee, members were given a fascinating introductory talk, by Mr. N. W. Grey, to the film of the construction of the Wilhemshaven to Cologne pipeline.

The Medway section were particularly pleased to have with them Mr. Hinde, Chairman of the South London Branch, who gave the vote of thanks, which was seconded by Mr. Morgan, Secretary of the Institute of Petroleum.

This joint meeting emphasised the close relationship between the welding industry and the petroleum industry and the 50 members present agreed that a most useful purpose had been served by bringing together the branches of the two industries.

A.M.C.

South Western — Non-destructive testing

On Monday, 6th March, at Radiant House, Bristol, Mr. H. Gibbs of the Admiralty, Bath, an acknowledged specialist in his subject, presented a lecture entitled "*Non-destructive testing in shipbuilding and marine engineering*".

Mr. Gibbs commenced by mentioning problems arising from the widespread use of welded construction for ships' hulls and marine engineering components. Brief references were made to methods of inspecting castings, forgings, and machinery components.

After outlining the requirements for satisfactory welded connections and the types of defect likely to be encountered, the basic principles of four post-weld inspection methods were illustrated. These included: magnetic crack detection and dye penetrants, radiology and ultrasonic flaw detection.

The limited use of each method and possible advantage of complementary use of two or more methods was described in relation to typical weld connections in highly stressed structures. Maintenance testing for fatigue failures, pipe wall thickness, etc., and routine examination of castings was also described.

Replying to questions, Mr. Gibbs said that submarine hull structures were 100% radiographically examined, whilst with surface ships such examination was made virtually for "welder control" purposes. He thought that five years as a radiographer was fair background experience for selection of an operator to be trained in the use of the ultrasonic flaw detection method of inspection.

In proposing a vote of thanks Mr. E. M. Wilson commended Mr. Gibbs on the clarity with which he had described the basic principles of each of the four post-weld methods of inspection mentioning too the excellent series of slides shown relating to the occurrence of typical weld defects.

E.M.W.

South Wales — Annual Dinner

The Branch held its Second Annual Dinner at the Angel Hotel, Cardiff, on Friday, 24th March, when Mr. D. B. Wilson, the Branch Chairman, presided over a gathering of about 120 members and guests, Mr. J. H. Groocock, Chief Engineer of the Steel Company of Wales Limited, proposed the toast of "The Institute", to which the response was made by Mr. R. G. Braithwaite, Past-President of the Institute, in the regretted absence of the President on account of health.

The Chairman proposed the toast of "The Guests", to which Mr. A. L. Beard, Chief Buyer of the Steel Company of Wales Limited, responded on behalf of a distinguished company, which included Mr. J. Strong, another Past-President of the Institute.

West of Scotland — Shipyard Organisation

On 15th February the Branch was addressed by the immediate Past-Chairman, Mr. D. B. Kimber, O.B.E., on the subject "*Some further thoughts on shipyard organisation*". In the previous session, a paper had been given by Mr. Straton describing developments

at Messrs. Austin & Pickersgill's shipyard, and Mr. Kimber expanded the theme by discussing the principles and limitations governing the design of completely new works and the expansion of existing yards. With the basic premise that shipbuilding is fundamentally the same as any other sort of engineering construction, it was first essential to answer two questions before works layout could be considered, viz:

(1) Range of product (*e.g.*, class of ships and size) and
(2) Rate of output (*e.g.*, number of ships per year, and from how many berths).

If these questions are answered, the rate of working can be estimated and the shop areas required can be examined by:

(1) Detailed time studies
(2) Surveying other peoples reorganisation, or
(3) Site layout may limit shop area and thus maximum output which can be achieved.

The facilities required were then discussed in some detail, both for steelwork and fabrication, and also berth layout, illustrated by examples from Continental yards (some chosen mainly because they were less widely known than British examples, but others because there were no equivalent layouts in Britain). The need for a higher standard of material supplied, and cheaper equipment was thought to be essential if automatic methods of fabrication were to be extended, and the adoption of such methods would lead to a considerable increase in the ratio of design and planning staff to 'shop floor' personnel. Similarly, for effective redesign of a shipyard, adequate staff had to be provided, free from other responsibilities.

This stimulating paper was much appreciated by the audience, many of whom were from the shipbuilding industry, and an interesting discussion ensued, ranging from Russian achievements in applying automatic welding in shipyards to provision of material-handling facilities, and whether ship design should be governed by fabrication methods or *vice-versa*. A vote of thanks, proposed by Mr. R. Walker, was heartily supported by all present.

H.H.M.

Wolverhampton — **Austenitic–Ferritic Joints**

Well over 50 members and guests crowded into the Old Wulfrunian's Club room to hear an interesting talk by Mr. F. S. Dickinson, on an aspect of welded fabrication which has seldom hit the headlines, but which has nevertheless been the subject of a tremendous research effort. Joints between austenitic and ferritic steels occur in such applications as high pressure steam piping or where a steel supporting structure is welded to a chemical vessel. Mr. Dickinson said that the main problems arose from:

(1) Metallurgical factors in making the joint
(2) The effect of differential expansion under cyclic loading conditions
(3) Preferential corrosion from atmospheric oxygen in the presence of a stress concentration.

With the aid of a modified Schaeffler diagram he showed that in the case of butt joints welded by the manual metal-arc process (having a dilution of about 35%) a 25/20 Ni–Cr electrode would give an effective buttering layer having a composition of approximately 20/15. If an 18/8 electrode was used the resultant weld metal might fall into the martensitic region with a tendency to cracking but this could be prevented by using an 18/8 electrode containing about 3% Mo giving a weld metal ferritic content of 15–20%. The use of stabilised electrodes helps to prevent migration of carbon from the ferritic to the austenitic steel.

Mr. Dickinson described a number of interesting methods of reducing the effects of differential expansion. One method is to choose a weld metal having a coefficient of expansion approximately halfway between those of the adjacent materials. Alternatively, a transition piece may be incorporated so that the differential stresses arising in each joint are approximately half those obtained in a direct joint. In each of these cases, it is necessary to use a niobium-stabilised steel to prevent carbon migration. Various techniques for welding a 2¼/1 Cr–Mo pipe to an 18/8 Cr–Ni pipe were described in detail, the most favoured method being to make use of a buttering layer before completing the joint. Mr. Dickinson also showed slides of a number of forged and extruded scarfed joints in which a nickel interface served the dual function of preventing carbon migration and helping to obtain a sound bond. Unfortunately, such joints were invariably extremely expensive.

Research work carried out to determine the effect of thermal cycling of various joints was then described. Some of the interesting results obtained are as follows:

(1) The time for which the joints were held at the peak temperature was critical in the range 550–650°C. For example, where the joint was held at 600°C. for 10 min in each cycle the expected life of the joint was 9 times that obtained when the joint was held at 600°C. for 20 hr in each cycle. Cracking occurred much more quickly at 650°C. than at 550°C. and also particularly in the presence of a small amount of oxygen
(2) Stress relief gave no increase in joint life
(3) In the absence of thermal cycling a very long life without cracking was obtained.

Mr. Dickinson concluded on a humorous note when he recalled that all the work of the designer, metallurgist, and welding engineer could be brought to nought if an elaborately prepared and costly shop-produced transition piece was sent to site and then welded into the pipe system back to front.

In opening the discussion, Mr. S. H. Griffiths, who had been welcomed back by our Chairman after his recent illness, mentioned that thermal cycling tests carried out on joints in 4 in. bore pipe had been taken to 10,000 cycles without failure. Mr. Dickinson agreed that failures usually occurred in the larger diameter heavy walled pipes where differential stresses were rather higher. Mr. Griffiths also spoke of a new electrode mentioned by Mr. Dickinson. Whilst this had improved welding properties he felt that the high Mn content might impair the corrosion resistance of the deposit.

Mr. A. Clements spoke of the benefits which arose from the elimination of the backing ring recommended by the speaker, who agreed that where suitably skilled welders were available considerable economic advantages were apparent. In response to a question from the chair, Mr. Dickinson reminded the audience that his figures for dilution referred to manual metal-arc welding. In the case of automatic welding, dilution could be in the range 50–60%.

In proposing a hearty vote of thanks, which was seconded by Mr. H. C. Wheeler, Mr. J. S. Allen thanked the speaker for dealing with a metallurgical subject in such a lucid way that engineers could understand it. However, he still felt that there was a strong case for flanged and bolted joints!

B.K.B.

INTERNATIONAL INSTITUTE OF WELDING

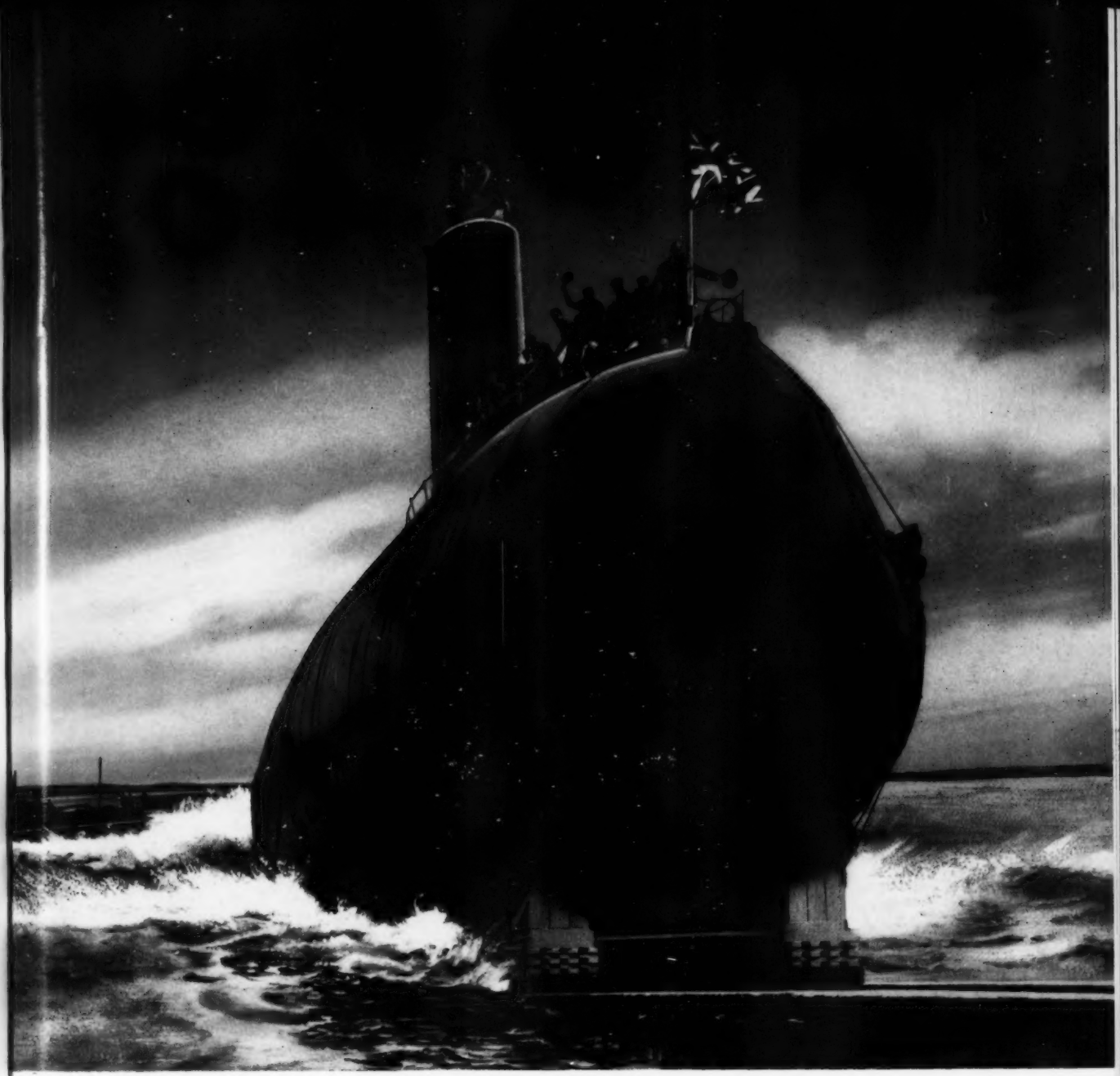
New Vice-President

At its meeting on 18th April, the Governing Council of IIW elected Professor J. Čabelka (Czechoslovakia) Vice-President of the International Institute, in succession to Professor H. Kihara (Japan).

1961 Annual Assembly – U. K. Delegation

A brief report on the Assembly is given on p. 36s of this issue (*International Welding*).

Though the British Delegation was only about half as numerous as usual, the United Kingdom was represented on all fifteen Commissions. As usual, Mr Robert Jenkins led the Delegation and together with Dr. R. Weck and Mr. G. Parsloe, represented the U.K. on the Governing Council.



MUREX electrodes first...

H.M.S. Dreadnought

MUREX

Over one million Murex electrodes of the "Fortrex" type were used for the welding of the *Dreadnought*, Britain's first nuclear submarine. These electrodes were specially developed in the Murex laboratories for the Admiralty and they were the only electrodes used for the manual arc welding on this great new submarine which is the largest ever to be built in Great Britain. The *Dreadnought* has been constructed for the Admiralty by Vickers-Armstrongs (Shipbuilders) Ltd., at Barrow-in-Furness, and she marks a new phase in the long and distinguished history of the Royal Navy.

MUREX WELDING PROCESSES LIMITED, WALTHAM CROSS, HERTS.

- *Twenty "off-the-shelf" models — stationary and traversing — prompt delivery.*
- *Full range from 3 tons to 50 tons.*
- *Construction embodying standard interchangeable assemblies permits later conversion of stationary to traversing units, or addition of extra units to increase capacity.*
- *Distance between roll frames, on both fixed and traversing units, can be adjusted to provide clearance for manholes or pipe connections on vessels being rotated.*
- *Infinitely variable speed ranges in 10:1 ratio (normally 6 in to 60 in per minute)*
- *Tachometer for speed indication fitted as standard.*
- *Preset protection against overload for drive gear.*

Versatility is the great point about BOC Roller Beds. Couple them with the BOC Ram Type Boom, and you widen the scope of production to include the circumferential and longitudinal welding of cylindrical vessels. Reduce setting-up times and increase output. Let us demonstrate.

THE BRITISH OXYGEN COMPANY LTD
Electric Welding Department, Quasi-Arc Works, Bilston, Staffs. Telephone: Bilston 41191

Among those who lectured at the meetings of the American Welding Society were:

Dr. A. A. Wells on "*The influence of residual stresses and metallurgical changes on low-stress brittle fractures in welding steel plates*", and Dr. W. S. Carlson on "*Pressure vessel design requirements in the future*".

NEWS FROM INDUSTRY

Welding plants for trainees

The hire service to industry of Rentweld Ltd. has been extended to include matched pairs of welding transformers for use in concerns that operate their own welders' training departments.

The pair consists of one 180-amp machine with two output ranges, each controlled by the same regular handle. One is divided into 10 steps of 10 amp for the novice, and the other is equipped with a fine regulator for the more proficient welder.

Power plant merger

The General Electric Co. Ltd. and Richardsons, Westgarth & Co. Ltd. announce that they have decided to pool their resources for the design, development, sales, and manufacture of heavy electricity generating and industrial power plant. The object of this arrangement is to secure an increase in their share of the home and export markets and at the same time to make the maximum possible use of their combined technical resources and production facilities.

John Thompson reorganisation

John Thompson Industrial Constructions Ltd. ("Induscon"), a member of the John Thompson Group of Companies, has taken over the administration of the John Thompson Conveyor Company Ltd. Mr. A. T. Shaw, Managing Director of "Induscon" will now direct the activities of the John Thompson Conveyor Company.

Before this reorganisation the Conveyor Company was regarded as a department of the John Thompson (Wolverhampton) Ltd., which, in turn, is a wholly owned subsidiary of John Thompson Ltd. "Induscon", also a subsidiary of John Thompson Ltd., has its head office at Tavistock House East, Woburn Walk, Tavistock Square, London, W.C.1.

The change is purely an administrative one, and the existing premises of the John Thompson Conveyor Company at Manor Road, Ettingshall, Wolverhampton, will continue for all project, contract, and engineering work.

Coinciding with the reorganisation is the appointment as manager of the Conveyor Company of Mr. R. T. McGregor, who also continues as sales engineer of "Induscon".

Oldest profiling machine

Hancock & Co. (Engineers) Ltd., pioneers in the manufacture of oxygen cutting and profiling machinery, inaugurated a competition at the Engineering, Marine, Welding and Nuclear Energy Exhibition to find the oldest Hancock profiling machine in the world. Its owner will be presented with a new OI U-arm profiling machine, and the winning machine is to be given to the Museum of Science and Industry at Birmingham.

The closing date for entries is 31st August, 1961. The new profiling machine will be presented to the winner at the Hancock factory in Croydon on 29th September, 1961, and the presentation to the Birmingham Museum will take place shortly afterwards.

Firms wishing to compete are asked to write to the manufacturers who will send their representatives to inspect the entered machines.

NEW PLANT AND EQUIPMENT

Submerged-arc welder

A mechanised submerged-arc 'squirt' welder, manufactured by the Lincoln Electric Co. of Cleveland, Ohio, is being marketed in Britain by Armco Limited, London.

In this versatile semi-automatic unit wire and flux are continuously fed to the welding gun from the flux container and reel mounted on the unit. The gun is traversed automatically at variable speeds up to 70 in./min by a small motor mounted on the gun itself.

The 'Idealarc' ML-3 enables a welding operator to carry out butt and fillet welding on plate thicknesses from $\frac{1}{8}$ in. to the largest sections normally fabricated.

Continuous-duty electrode holders

Long periods of welding with $\frac{5}{16}$ in. electrodes at 600 amp creates serious discomfort for the operator unless the electrode holder is designed especially to give adequate heat dissipation from the handle.

Two new holders have recently been marketed in Britain for use at high

currents. The Interlas Terrier 600 Special feeds the welding cable direct to the electrode grip and has a separate metal-shielded handle. The Courtburn 'Air Insulated' holder, on the other hand, incorporates heat-resistant distance pieces to separate the handle from the inner cable and its connection to the electrode grip.

Welded beam connections

Saxe welded connection units, used for several years in the U.S.A. for providing positive location of beams on site, are now being marketed in Britain by King Aircraft Corporation (K.A.C. Ltd.) of Glasgow.

Saxe fittings eliminate the need for temporary bolting of beams to girders or columns and provide secure support until welding is completed. Seatings are tested to 50,000 lb and both web clips and seatings are self-locking. The fittings have forged-in centrelines.

Aluminium for shipbuilding

Alcan Industries Ltd., formerly Northern Aluminium Co. Ltd., have recently put on the market an improved Al–Mg alloy, Noral D54S, which is now offered as a standard material for both plates and sections for structural use to the requirements of Lloyd's Register of Shipping.

Although, in the past, plate material to NP5/6 specification has met the minimum mechanical properties laid down by Lloyd's Register for aluminium structural alloys it has been necessary to supply extruded sections to the NE6 specification in order to guarantee these properties. Noral D54S, with a nominal composition of 4½%Mg, ¾%Mn, and ⅛%Cr, will meet Lloyd's requirements both in the plate and extruded forms.

This applies in the annealed (O) condition as well as the 'as manufactured' (M) condition, leading to the further advantage that the properties after welding do not fall below the minimum properties specified for the material as supplied.

Noral D54S, which is generally similar to the American alloy AA5083, falls within the range of chemical composition covered by the NP5/6 plate specification but is not yet covered by a British Standard for extruded sections.

New nickel steels

A new 18% nickel alloy steel with unmatched toughness at the highest strength levels of traditional alloy steels has been developed by The International Nickel Company, Inc.

It is claimed to be the only known material which has the ability to achieve a yield strength in excess of 250,000 lb/sq.in. while maintaining a nil-ductility temperature below −80°F. Another outstanding characteristic of 18% nickel steel is its excellent notch tensile strength which exceeds 400,000 lb/sq.in. (measured under the most severe test conditions with a notch radius of 0·0005 in.) Tests have shown that this new alloy possesses a remarkable resistance to delayed cracking when exposed to a severe corrosive atmosphere in a highly stressed condition.

The new steel develops its high strength, ductility, and toughness by means of an easy heat-treatment involving age-hardening of martensite. This treatment has been given the abbreviated description of 'mar-ageing'.

The new steel has a nominal composition of 18% Ni, 7% Co, 5% Mo, <0·5% Ti with a maximum of 0·05% carbon. Higher and lower tensile strength can be obtained by modification of this basic composition.

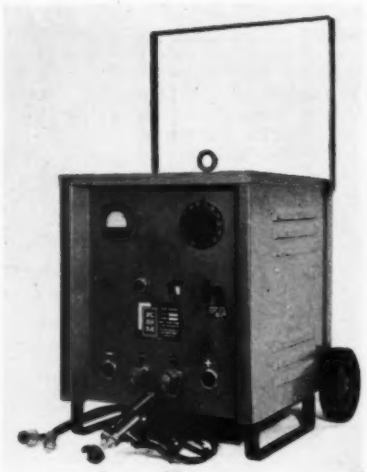
The 18% nickel steel can be readily welded by either manual or automatic methods. Sound, crack-free welds are achieved, even on material in the fully-heat-treated condition, without preheating. Post-weld mar-ageing restores the softened heat-affected zone of the parent plate to full strength and establishes in the weld metal properties closely approaching those of the base plate.

Capacitor discharge stud welding

A recently formed company, K.S.M. Stud Welding Ltd., of Old Woking, Surrey, a subsidiary of the American company of the same name, is now producing all-British manufactured stud welding equipment.

Many welding operations require large quantities of current to be discharged in a brief period of time, and this is generally true of stud welding. The consequent high load on the power supply and the low efficiency have been overcome in the K.S.M. equipment by the use of the principle of capacitor discharge.

Power is obtained from a single phase 13 amp a.c. supply which is then rectified and is used to charge a large bank of capacitors. The voltage of the charge can be selected to suit the welding conditions. A specially designed contactor discharges the capacitors to the welding gun and automatically ensures their recharging some 10–12 times per minute.



The studs, which can be made in various materials and to a wide range of shapes, require no ferrule but are formed with a small "pip" in the centre of the head. In the normal manually operated gun, the stud is held by spring loading with the "pip" just touching the workpiece, and on release of the press switch it is forced on to the parent material at the same time as the "pip" is vapourised by the welding current. This combined pressure and instantaneous heat forms the weld, which is so localised that no distortion or upset appears on the reverse side of very thin sheet.

At present the K.S.M. equipment will operate on a range of stud diameters from 6 B.A. to ¼ in. but in this range attachments can be made to much thinner plate than is possible with the conventional arc-stud welding equipment.

The simplicity of the equipment (no timing device is needed and stud loading is positive and quick) and its low initial cost should make it a versatile tool in any industry, large or small, where multiple small attachments are needed.

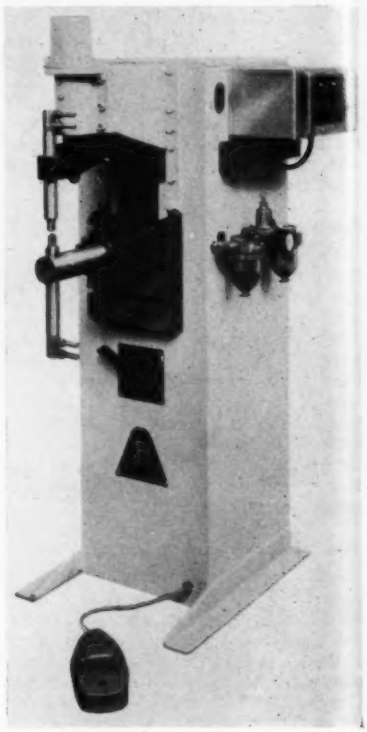
Where the design includes set patterns of attachments a multi-head bench unit can be used, with semi-atttomatic control.

This new British company is not only able to call upon the resources of its American counterpart for information and development but the members of its technical staff are acknowledged experts in the field of stud welding, and are able to advise on its applications.

Universal resistance welder

A low-capacity vertical sliding head and air-operated welding machine for spot, projection, and stitch welding, has been added to the range of equipment supplied by Standard Resistance Welders Ltd.

Three sizes of this design are available at nominal ratings of 25, 35, and 50 kVA



at 50% duty cycle, and the low-impedance transformer is designed to operate on 2 phases of a 400/440 V, 50 cycle, 3-phase supply.

The welding cycle is controlled by a 4-stage electronic timer in conjunction with an ignitron contactor. Heat is regulated by a 6-stage off-load rotary switch. Maximum electrode pressure can be attained with an 80 lb/sq.in. air supply.

CORRECTION

Brittle fracture of steels

It is regretted that Dr. Tipper's report published in the May issue of *BWJ* (1961, vol. 8, p. 280) the two groups of blocks for Figs. 2–5 and Fi4s. 6–9 have been transposed.

In reading the report the two lines of captions should be reversed.

Current WELDING LITERATURE

Book Reviews
Additions to the Institute Library
Trade Publications

CONTENTS OF PERIODICALS RECEIVED

This section is intended to give a survey of the current welding literature received by the Institute of Welding Library. The contents lists are not exhaustive; only the main articles in welding journals are listed, and reprints from other journals and short notes are generally excluded. In addition, welding articles from other periodicals are listed. Abstracts of welding literature are given in the Bibliographical Bulletin of Welding and Allied Processes, *published by the International Institute of Welding, and details of this may be obtained from the Secretary of the Institute of Welding.*

Welding Journals

Canadian Welder, 1961, vol. 52, February

Non-destructive testing of pressure vessels, J. Battema (10–16)
"Square-butt" welding (20)
Welding for largest radio transmitter (21)

ESAB Revue (France), 1960, No. 3–4

Quality of welds by electro-slag process at different stages of the treatment, M. I. Wachtmeister (1–16)
Gas shielded arc welding, M. Per Sjoman (17–32)

Journal of the Japan Welding Society, 1961, vol. 30, January

Fundamental studies and industrial practice in cold pressure welding. Part 1, T. Saito and K. Yamazi (3–10)
Impressions of Europe, 1, M. Inagaki (11–22)
Welding conditions of CO_2–O_2 shielded arc welding. Report 1. Relation between welding conditions and oscillogram of arc, H. Sekiguchi and I. Masumoto (23–29)
The shear fatigue strength of welded beams, Y. Tada and others (30–43)
Study on the phenomena of inert gas metal arc welding by high speed motion pictures. Report 5. CO_2–O_2 shielded arc welding, I. Ukita and others (44–48)
Study on stress corrosion cracking of austenitic stainless steels Report 1, M. Watanabe and others (49–53)
Stud welding of steel pipe to steel plate at right angles, I. Otsuka (54–58)
Study of a powerful spot welder which is tentatively made by using electrolytic condenser to store and discharge energy, I. Otsuka and T. Hatsusiba (59–63)

Lastechniek (Netherlands), 1961, vol. 27, No. 3L, March

Impact and explosion tests on welded tanks, C. Vollers (44–51)

(*Philips*) *Welding News* (Holland), 1960, No. 116, November–December

Manual arc welding with coated electrodes, W. Moerman (2–9)
Welding of steel anchor bars for sluice gates of the spillway in the Dam of Barcena, Spain, J. Callejo (10–12)
Use and misuse of automatic welding machines in shipbuilding and ship repairing yards, A. A. Barbosa (14–17)

(*Philips*) *Welding News* (Holland), 1961, No. 117, January

Manual arc welding with coated electrodes. Part 2, W. Moerman (2–6)
Enclosed welding of shafts to steel discs, J. Callejo (7)
Deep penetration electrodes, B. Zouthberg (9–11)

Revue L'Air Liquide (France), 1960, vol. 8, Winter

Mass fabrication of trailers in stainless steel (9–16)
An automatic machine for oxyacetylene welding of tubes for electrical installations (19–24)

Schweisstechnik (Austria), 1961, vol. 15, No. 1, January

Choice of current for automatic welding, Ing. Tajbl (1–5)
Raising welding output of autogenous welding (of sheet metal), H. H. Reinsch (5–8)

Schweisstechnik (Austria), 1961, vol. 15, No. 3, March

Current for resistance welding, H. Moditz (25–33)

Schweisstechnik Soudure (Switzerland), 1961, vol. 51, February

Aluminium in construction work (34–39)
Lead poisoning and its prevention (40–41)

Soudage et Techniques Connexes (France), 1961, vol. 15, January–February

Calculation of the thermal processes of welding, N. N. Rykaline (5–38)
Recommended practices concerning radiographic image quality indicators (I.Q.I.), International Institute of Welding Commission V (39–42)
Study trip to Germany of the 29th Year of the E.S.S.A. (55–69)

South African Institution of Welding Monthly Bulletin, 1961, No. 91, February

How weldable is S.G. iron? (2–6)

Welding Bulletin (New Zealand), 1960, No. C.34, December

Forestry is forever (2–10)

Precision by the ton (14–16)

Welding Engineer (U.S.A.), 1961, vol. 46, February

Oxygen, its production, distribution and uses (25–27)

Economical oxygen distribution, storage a must as demands soar, G. Cannizzo (28–30)

Oxygen pressure reducing regulators, H. A. Goodwin (34–37)

Diffusion bonding, brazing join copper parts (46)

Vapor shielding process cuts welding time (54)

Welding Journal (U.S.A.), 1961, vol. 40, March

Nondestructive testing is key to future of welding quality control, G. J. Gibson (225–228)

Metallizing nickel powder involves vacuum blasting to prepare steel before surfacing with metal-spray gun, W. B. Keyser (229–231)

Vacuum melting produces cleaner, better performing high-temperature brazing alloys, C. R. Benson (232–236)

Today's industrial role of magnetic-flux gas-shielded arc welding, R. T. Telford (237–243)

Lathe redesign in welded steel achieves cleaner lines and lower cost, E. Duesing (244–248)

Some factors affecting the notch toughness of steel weld metal, K. E. Dorschu and R. D. Stout (97*s*–105*s*)

Properties of mild steel weld metal not revealed by the All-Weld-Metal tensile test, K. Winterton (106*s*–109*s*)

Evaluating crack-resistant electrodes for welding HY80, S. I. Roberts (110*s*–112*s*)

Notched longitudinal-bend specimens for evaluating armor weldments, S. M. Silverstein, R. P. Sopher and P. J. Rieppel (113*s*–117*s*)

Brazing of beryllium for high-temperature service, S. R. Maloof and J. B. Cohen (118*s*–122*s*)

Recent progress in development of self-fluxing, air proof brazing alloys, N. Bredzs and H. Schwartzbart (123*s*–129*s*)

Use of the centre notch-tensile test to evaluate rocket-chamber materials, J. J. Warga (130*s*–134*s*)

Catastrophic fracture caused by ductile-to-brittle transition under residual stress, W. N. Platte (135*s*–137*s*)

Theory of brittle crack initiation and propagation—a theoretical analysis of ESSO test, Y. Akita and K. Ikeda (138*s*–144*s*)

Welding News (Australia), 1961, Folio 105, February

Handigas—processes and procedures (2–7)

Porosity control in submerged arc welds, R. A. Stringer (15–18)

Low hydrogen electrodes make critical repair (19)

Simple joints in copper tubing (20–21)

Weld estimating data (22)

Zvaranie (Czechoslovakia), 1961, vol. 10, January

Fatigue strength of welded motor car disk wheels, O. Izdinsky (3–5)

Welding of 5% Cr molybdenum or tungsten conditioned steels, V. Pilous (5–9)

Resistance welding of steam boiler tube systems, J. Vintr (10–14)

Vacuum brazing, V. Horacek (14–17)

Automatic under-flux welding of austenitic alloy steels, A. Missikewitsch (18–20)

Zvaranie (Czechoslovakia), 1961, vol. 10, February

Weldability tests on gas-welded thin steel sheets, M. Basista (34–37)

Quality control of resistance welded joints in thin sheets, M. Lipa (37–42)

Under-flux welding of aluminium, J. Vana (42–46)

Exploitation of scrap from circular oxygen cutting in maintenance and repair service, J. Vodera (47–48)

Other Journals

Titanium sheet forming, A. T. Carse and W. T. Ardis (*Metal Industry*, 1961, vol. 98, 17 February, pp. 131–132)

An unusual construction method in steel, J. R. Lowe (*Building with Steel*, 1961, vol. 1, No. 5, pp. 30–32)

The welding of aluminized steel sheet, T. M. Molossi and J. H. Nicholls (*Sheet Metal Industries*, 1961, vol. 38, February, pp. 95–98)

Stellite powder welding for hardfacing (*Machine Shop Magazine*, 1961, vol. 22, February, p. 105)

A note on brittle fracture initiation in mild steel by prior compressive pre-strain, C. E. Turner (*Journal of the Iron and Steel Institute*, 1961, vol. 197, February, pp. 131–135)

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Welded titanium tubing curbs corrosive attack of acids (*Iron Age* (U.S.A.), 1961, vol. 187, January 12, pp. 68–69)

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New construction makes use of automatic welding (*Canadian Machinery and Metalworking*, 1961, vol. 72, February, p. 122)

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Design implications of creep in pressurized cylindrical shells, I. Rattinger and J. Padlog (*Aerospace Engineering* (U.S.A.), 1961, vol. 20, March, pp. 26–27; 97–107)

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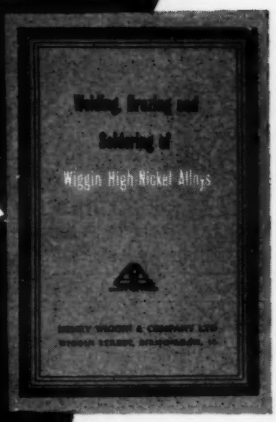

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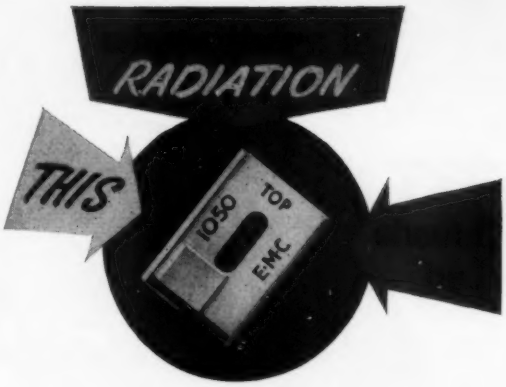

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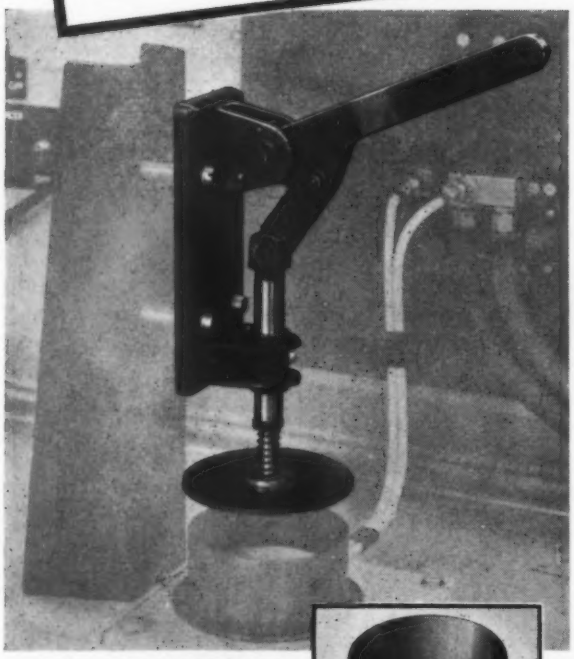

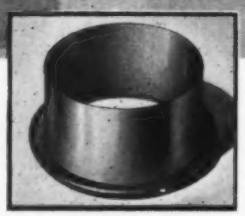

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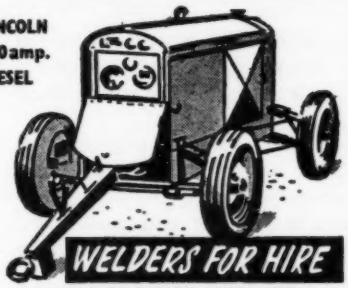

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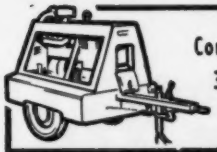

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ADVERTISERS' INDEX

Arc Manufacturing Co. Ltd.	23
Armco Ltd.	7
Associated Electrical Industries	4
Brauer, F., Ltd.	28
Brent Chemical Products Ltd.	15
Bristol Aircraft	28
British Oxygen Co.	*Outside back cover*, 16, 20, 22
Butterfield, W. P.	26
Classified	29, 30
Courtburn Supplies Ltd.	26
Crompton Parkinson (Stud Welding) Ltd.	10
Distillers Co. Ltd., The	18
English Electric Co. Ltd.	*Inside front cover*
Eutectic Welding Alloys	12
Gamma-Rays Ltd.	15
Hancock & Co. (Engineers) Ltd.	13
Harvey, G. A. & Co. (London) Ltd.	1
Hirst Electric Ltd.	27
Ilford Ltd.	5
Imperial Chemical Industries Ltd.	14
Inspection Equipment Ltd.	15
Inspection Services Ltd.	15
Johnson Matthey Ltd.	11
Lanarkshire Steel Co. Ltd.	8, 9
Manchester Oil Refinery (Sales) Ltd.	15
Murex Welding Processes Ltd.	21
Palmer Aero Products Ltd.	15
Radiation Monitoring Service	26
Research and Control, Instruments	*Inside back cover*
Robey & Co.	3
Rockweld Ltd.	24
Saturn Industrial Gases	2
Smith Hugh & Co. (Glasgow) Ltd.	6
Suffolk Iron Foundry (1920) Ltd.	27
Ultrasonoscope (London) Ltd.	15
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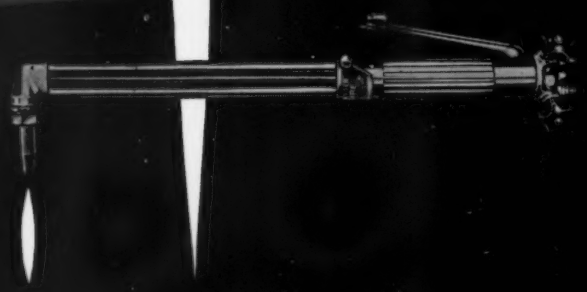

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